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FINAL REPORT

PROPULSION/ELECTRICAL POWER GENERATION

ENGINEERING OPERATIONS REPORT

JUNE 1972

MASTER

AEROJET NUCLEAR SYSTEMS COMPANY

A DIVISION OF AEROJET-GENERAL

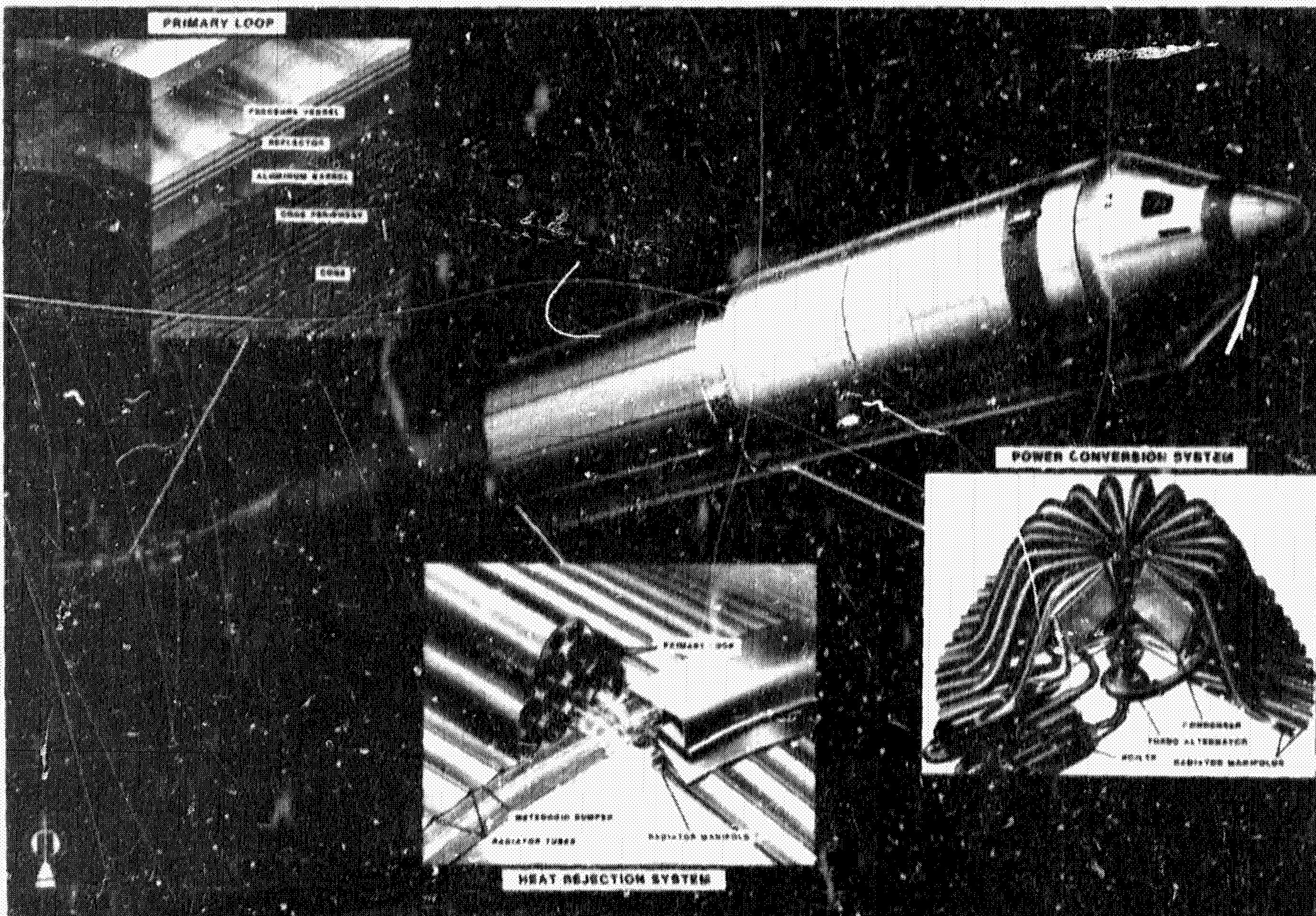
FOREWORD

The work reported herein is in fulfillment of Project 395 of Contract SNP-1. The period of performance was from October 1, 1970 through notice of termination of the NERVA Program on February 18, 1972, and engineering phase-down ending June 16, 1972.

Technical direction of Project 395 was by Mr. William H. Robbins of SNSO-Cleveland.

Westinghouse Astronuclear Laboratory Project 495 of Subcontract SNP-1 was a supporting study.

DUAL MODE NERVA



ABSTRACT

The 25 Kw electric NERVA Dual Mode System study was continued. The basic system was essentially unchanged from the system originally reported. The operation of the system during the engine cooldown period was altered to enhance the cooldown hydrogen saved thereby increasing the desirability of the dual mode concept.

A parametric study was conducted to determine the most advantageous size (electrical) for the Dual Mode NERVA System. The result, based upon cooldown hydrogen savings only, was a 10 Kw electrical system.

Concepts potentially important to future nuclear rocket engine dual mode systems are discussed.

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FIGURES

I. SUMMARY

The feasibility of utilizing the NERVA engine as a heat source for a 25 Kw(e) electrical power system was investigated. A minimum number of additions to the NERVA engine to perform the dual function was maintained as a basic ground rule. Several concepts for removing heat from the engine in addition to those of Reference (1) were evaluated. The final system selection was not changed however. It consists of a closed loop, independent of the normal NERVA flow path, primary coolant circuit which carries heat from the engine to the forward end of the stage where the hydrogen coolant supplies heat to a boiler (vaporizer). An organic working fluid, thiophene, was selected as the most desirable fluid for the Rankine-cycle power conversion system. A wick-type condenser is used for the zero gravity environment. The heat rejection system utilizes gaseous hydrogen flowing in closed parallel loops to conduct heat from the condenser to the exterior of the stage.

Off design operation of the system during the early portion of the cooldown for the NERVA engine was investigated to enhance the cooldown hydrogen savings.

Consideration of dual mode systems larger and smaller (see references at end of report) than the 25 Kw(e) system showed that a 10 Kw(e) resulted in the maximum payload for a lunar mission provided the need for electric power was not more than 10 Kw(e). In other words, the dual mode system size optimization was based upon cooldown propellant savings only.

( ) Indicates reference at end of report

System implications for application of the dual mode concept to other nuclear rocket engines is discussed. Also, discussed is the effect of the Earth Orbiting Shuttle on the dual mode concept.

II. INTRODUCTION

This report is intended to be useful for bridging the gap between the NERVA Dual Mode System and future nuclear rocket engines which may retain the dual mode concept. It is important to note that the now terminated NERVA engine program was never directed to utilize the Earth Orbit Shuttle (EOS). The future of nuclear rockets will be at the Los Alamos Scientific Laboratory (LASL) where work is under way for a smaller thrust (20% of NERVA) engine which has great potential for unmanned deep space missions.

The LASL engine design is in the preliminary design stage. It differs from the NERVA engine in many ways some of which are important to the dual mode concept. For example, an aluminum reflector cylinder used for the primary coolant loop heat exchanger is not required in the LASL engine design. Thus, the dual mode coolant circuit will utilize the core structural support system as its incore heat exchanger. This feature is expected to reduce primary coolant loop flow impedence and permit increased coolant temperatures in the primary loop. These are both very desirable features for the dual mode concept.

The basic NERVA Dual Mode System, described in Reference (1), was retained essentially unchanged during the period of this report. No significant improvements in hardware design were achieved. The off-design operation of the NERVA Dual Mode System during engine cooldown resulted in greater cooldown savings. The concept was to increase radiator temperature temporarily to enhance heat rejection capability at the expense of electrical power output. After the residual (decay) power reached normal dual mode system heat input levels, return to normal electrical power operation was performed.

The concept of using a rocket engine for propulsion and for generation of heat which may then be converted into electric power and many other potential uses is a new concept. Chemical rocket engines have been very successful in providing propulsion only. During chemical rocket engine operation it would be possible to provide heat for other applications. However, since rocket engine operation is normally a very short time period in comparison with normal space flight times, it is impractical to utilize the chemical rocket engine for supplying energy to the space vehicle.

The nuclear rocket engine is unique in that it contains an enormous supply of energy in its nuclear fuel. (The amount of propellant, hydrogen, is limited.) Utilization of more of the energy inherent in the nuclear rocket engine is an objective of the work reported herein.

If it is practical to design a nuclear rocket engine with the capability of providing propulsive energy and non-propulsive energy, it may be a requirement to do so in order to make the nuclear rocket engine competitive with the chemical rocket engine. The point is simply that the high (nearly infinite from a practical standpoint) inherent energy contained within the nuclear rocket engine involves a significant weight and cost which the chemical rocket engine does not have. The added weight is significantly offset by the ability to use hydrogen as the propellant thus providing a much higher exhaust velocity and correspondingly lower propellant consumption. The cost remains a detriment to the nuclear rocket system unless some accounting for the non-propulsive energy capability of the nuclear rocket engine is made.

Space electric power systems may utilize the energy of the sun directly or they may utilize chemical or nuclear energy. Solar cells are very practical for earth orbiting satellites. Chemical energy has been useful for fuel cells on the Apollo flights to the Moon. Long duration space power at low rates can be supplied by radioisotope nuclear power units such as the Alsep unit left by the astronauts on the surface of the Moon. Applications for space electric power at great distances from the sun and of moderate amounts will be supplied by reactor-dynamic power conversion systems such as the dual mode electric systems. Fortunately, these same mission applications may benefit from nuclear rocket engine propulsion. The United States does not have a reactor-dynamic power conversion system operating in space at the present time. Component and subsystem technology development is currently in progress. The diverse nature of these development programs make an accurate assessment of the future cost of operational systems difficult.

Ultimately, the decision to use chemical rocket engines plus separate nuclear electric space power plants as opposed to the use of a nuclear rocket engine with propulsion and electric power capability will depend upon the relative cost of the two systems and the time to supply them. At present, chemical rocket engine technology is in good shape. Nuclear rocket engine technology is growing fast but recent NASA decisions make its future much less certain than that of chemical propulsion. Space nuclear auxiliary electric power (SNAP) systems are developing at a slow rate. The SNAP programs are directed at improving the technology base as opposed to the development of particular units.

An interesting situation which is now present is the opportunity to initiate the development of a nuclear rocket engine which is much smaller in thrust and in gross weight as compared with the NERVA rocket engine. The specifications for this small engine are not yet firm thus the opportunity to include provisions for generation of heat for the vehicle during the non-propulsion portion of flight is available. It would seem that this is a "golden opportunity" provided the alternative mode of operation does not dictate unreasonable design requirements on the rocket propulsion system. In the case of the NERVA engine, an auxiliary system (primary loop) was devised which was completely separate from the normal engine flow circuit and the normal engine temperature limitations were retained for the non-propulsion mode. Certainly more latitude in the selection of materials for a new engine could permit increased operating temperatures during the non-propulsion mode and thereby improve the performance, reduce the weight and size of the electric power system.

The nuclear rocket engine also has potential for providing very large amounts of electric power (or shaft power) in space for relative short periods of time by using its hydrogen exhaust jet to drive a turbine or a magneto hydrodynamic generator (MHD). In this mode of operation the nuclear reactor provides the heat energy and the normal propellant feed system provides the high pressure hydrogen which drives the power device and then exhausts into space. Use of hot hydrogen as the working fluid has advantages over other working fluids because it has much higher available energy per pound and therefore requires comparitively less flow rate.

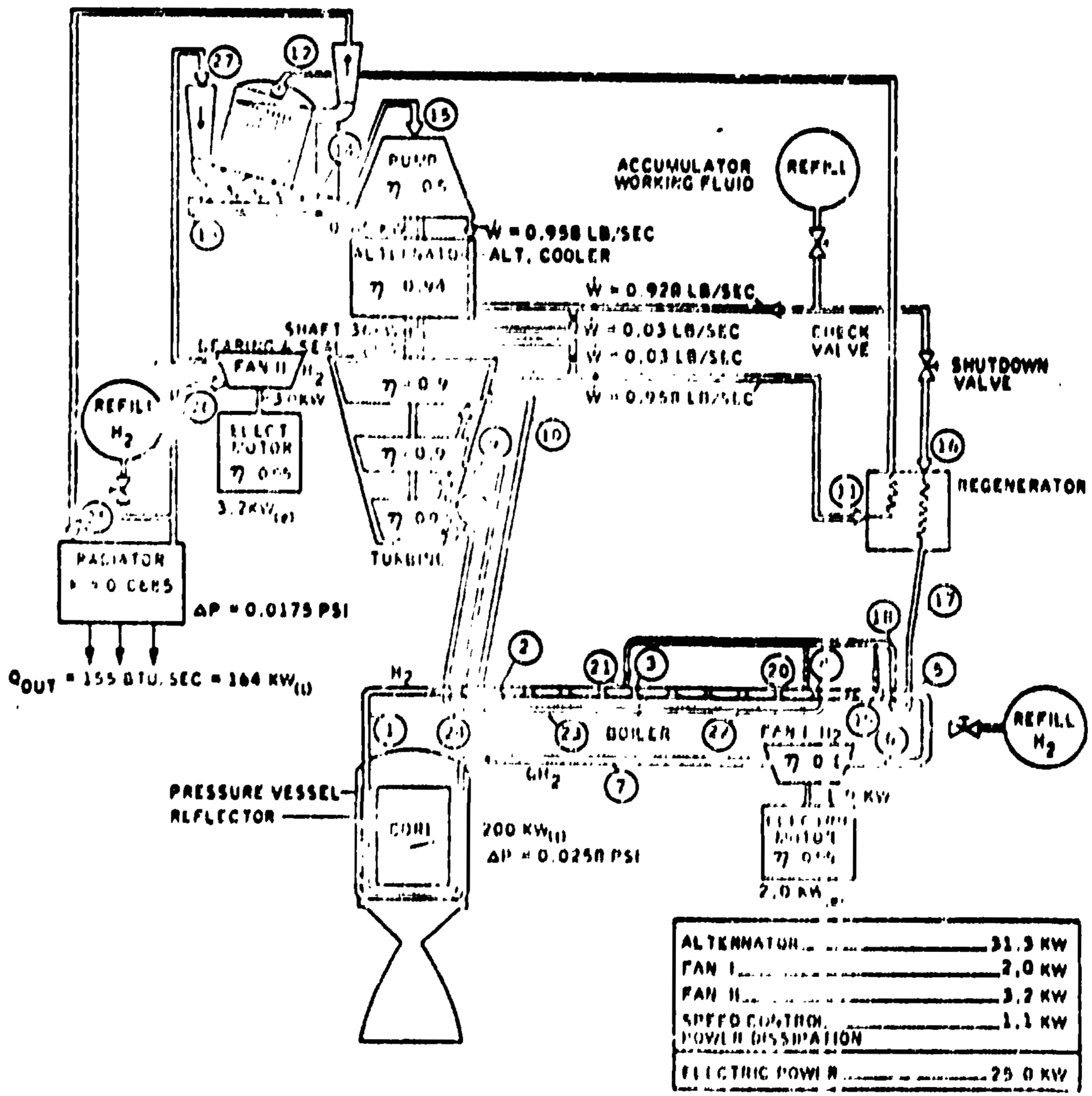
III. DESCRIPTION OF DUAL MODE SYSTEMS

A. NERVA System

The dual mode electrical system (shown schematically in Figure 1) includes a NERVA engine, a system for converting heat to electricity, and a circuit for rejecting waste heat from the power conversion system. Only small additions to the engine are necessary to provide a means of transporting low temperature, low power thermal energy from the engine to the power-conversion system.

1. Primary Loop

The technique of heat removed from the engine for electrical power mode is to circulate gaseous hydrogen through the metal parts of the reactor (i.e., within the pressure vessel) and duct the warmed hydrogen to the forward end of the stage where the heat is used to boil (vaporize) the working fluid of the power conversion system (see Figure 2). The cooled hydrogen is then compressed slightly to cause it to return to the heat source. The primary loop lines are shown running the length of the propellant tank in Figure 2. Rechargeable batteries can provide electrical power to the NERVA engine during rocket mode and the transition to electrical mode. After the transition the batteries can be recharged by the dual mode electrical system.



NO.	STATION INLET	FLOW LB/SEC	PRESSURE PSIA	TEMP °R
	PRIMARY H_2 LOOP			
(1)	SUPERHEATER	0.722	600	775
(2)	HIGH PRESS BOILER	0.722	-	770
(3)	INTERMEDIATE PRESS BOILER	0.722	-	748
(4)	LOW PRESS BOILER	0.722	-	722
(5)	PREHEATER	0.722	-	713
(6)	FAN I	0.722	599.5	699
(7)	REFLECTOR	0.722	600	700
	WORKING FLUID LOOP			
(8)	TURBINE	0.464	44.7	750
(9)	TURBINE	0.278	34.7	750
(10)	TURBINE	0.186	28.4	750
(11)	REGENERATOR	0.958	2.6	606
(12)	CONDENSER	0.958	2.48	560
(13)	DRIVE JET	0.928	75	541
(14)	JET PUMP	0.958	2.46	540
(15)	CENTRIFUGAL PUMP	1.968	8.1	540
(16)	REGENERATOR	0.928	75	550
(17)	PREHEATER	0.928	74	582
(18)	BOILER	0.928	73.5	686
(19)	LOW PRESS BOILER	0.186	28.9	636
(20)	INTERMEDIATE PRESS BOILER	0.278	35.2	606
(21)	HIGH PRESS BOILER	0.464	45.2	686
(22)	LOW PRESS SUPERHEATER	0.186	-	686
(23)	INTERMEDIATE PRESS SUPERHEATER	0.278	-	700
(24)	HIGH PRESS SUPERHEATER	0.464	-	720
	SECONDARY H_2 LOOP			
(25)	RADIATOR	2.22	599.6	540
(26)	FAN II	2.22	599.5	520
(27)	CONDENSER	2.22	600	521

Figure 1 - Dual-Mode Electrical Power Generating System

Figure 2

SEE FRONTISPIECE PHOTOGRAPH

2. Power Conversion System

Thermodynamic analyses have shown that the Rankine cycle is the only feasible cycle for the source and sink temperatures available to the dual mode system. A number of potential working fluids were evaluated and thiophene (C_4H_4S) was selected. This organic fluid has good thermodynamic performance, a dry turbine expansion, reasonable vaporizer and condenser pressures and outstanding chemical stability at the dual mode operational temperatures.

A multistage turbine is required to efficiently convert the expanding thiophene even though it has a molecular weight of 84. Turbine weight is a nearly negligible weight in comparison with the additional weight of the dual mode system hence it is prudent to strive for maximum turbine efficiency.

The zero gravity environment eliminates the use of static head of liquid to provide a head for moving condensed liquid from the condensing surfaces to the suction of the boiler feed pump. The mechanism selected for the organic liquid is capillary force. A wick on the condenser surfaces holds the condensing liquid and a jet pump accelerates the liquid from the liquid side of the wick into the inlet of a centrifugal feed pump. Some of the discharge from the centrifugal pump is recirculated for jet pump drive fluid. The jet pump supercharges the suction of the centrifugal pump thus preventing cavitation erosion damage or vapor lock.

3. Heat Rejection Loop

The function of the heat rejection loop is to transport waste heat from the condenser of the power-conversion-system to the outer surface of the space vehicle which then radiates the waste thermal energy. The type of system selected was a closed-loop(s), gaseous-hydrogen circulating system similar to the primary loop except that the heat rejection loops have many independent parallel circuits. The circulation of the high pressure hydrogen is produced by electric motor driven fan(s) in each loop. These fans may be switched on-off to provide heat rejection system control.

The magnitude of the thermal energy to be rejected is at least 80% of the thermal power of the primary loop. For system efficiency considerations it is desirable to maintain a small temperature difference (20°R) in the hydrogen flow as compared with the primary coolant loop temperature difference (75°R). This necessitates a correspondingly larger flow rate of hydrogen and larger flow areas. This was possible in the NERVA Dual Mode System design because the flow areas are not constrained in the heat rejection loop as they were in the primary coolant loop.

B. Small Nuclear Rocket Engine System

The following discussion is not intended to be a preliminary design of dual mode system for the small engine however the discussion should allow a more rapid focus on major design problems.

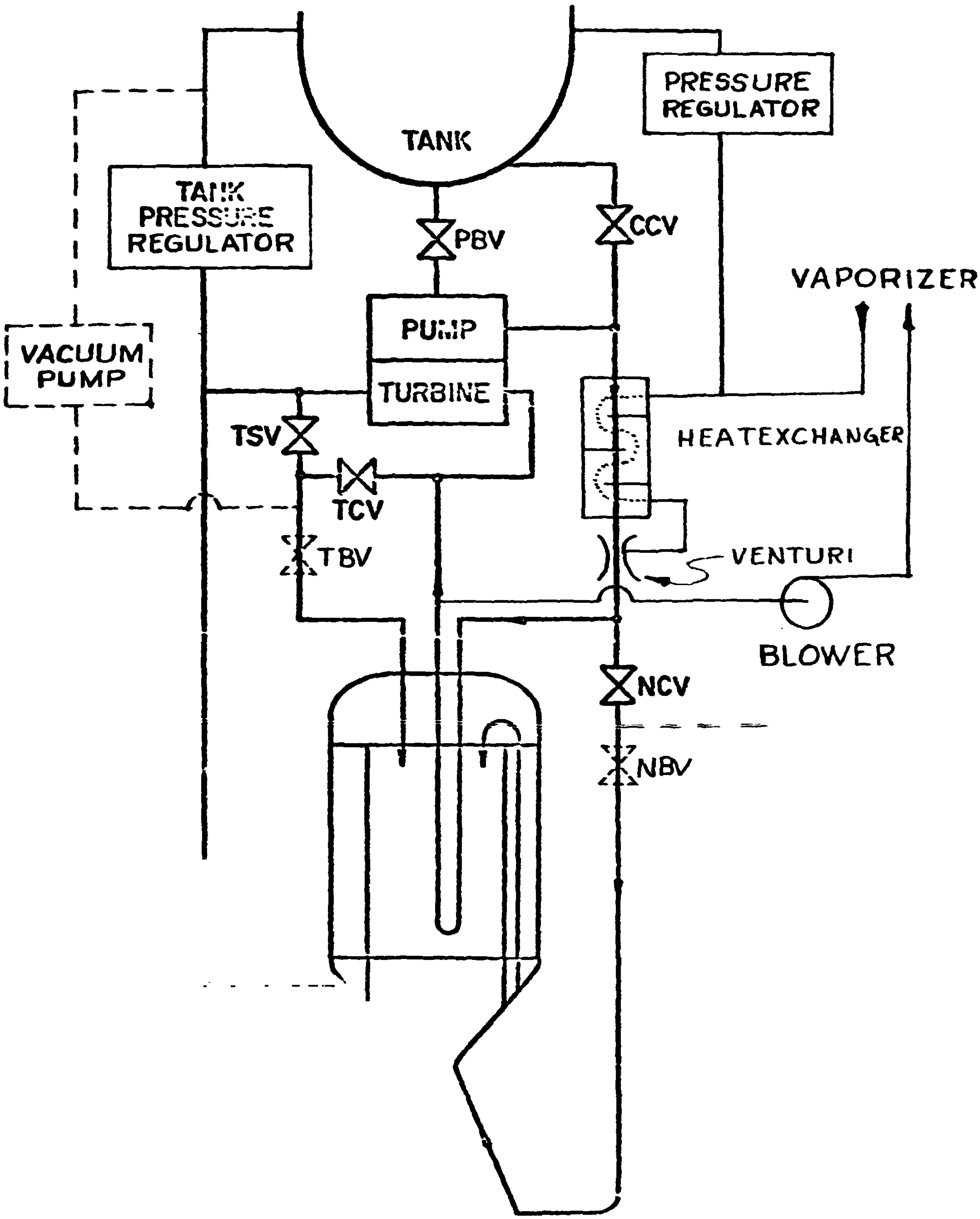
1. Primary Loop

The small engine reactor design has significant differences from the NERVA reactor so far as dual mode is concerned. Use of the core structural support system for the heat input exchanger of the primary loop is very attractive for several reasons. The flow area is comparitively large, the gas temperatures may be as high as 1000°R and the primary coolant flow rate may be such that only a small (25°R) coolant temperature rise is feasible with modest pumping power. This type of heat exchanger was considered initially for the NERVA Dual Mode System. However, it was discarded because inadequate sealing of the manifolds was inherent in the design. Attention to this design requirement from the beginning could result in a reactor design with a leak tight support system.

Not only must the incore heat exchanger be leak tight, the valves which shut-off the normal rocket mode flow circuit must also be leak tight. Figure 3 shows one possible flow scheme. It may be more practical to "double-up" on the valves which can leak through the engine nozzle. The chamber(s) between the doubled-up valves could be pumped down to maintain a very low pressure on the second or back up seals. This design feature would consume a small amount of electric power for pumping but it could greatly reduce overboard leakage and resultant loss of hydrogen.

The flow schematic of Figure 3 is interesting because it does not have valves in the dual mode portion of the primary coolant loop. Also, it may be feasible to operate the electrical power system during normal operation because the design temperature of the support structure outlet fluid could be the same temperature as that for normal dual mode operation. The return flow from the dual mode vaporizor can be cooled by the full rocket engine pump flow as illustrated in Figure 3. This feature may be needed to

Figure 3



SMALL ENGINE FLOW DIAGRAM
MODIFIED FOR DUAL MODE

reduce the nonuniform temperature of the coolant on its way to the tie rods or nozzle. It will also help to reduce the density variation in the venturi mixer.

The use of the venturi to induce flow of the primary loop does several things for the engine cooling system. First it avoids a requirement for a heat exchanger and the inherent delta temperature drop to drive the heat energy into the primary loop. Secondly, it negates the requirement for either two valves or another blower when operating betwee rocket engine firings. As the primary loop pressure is of the same magnitude as the engine pressure, there is no weight penalty for two different stress requirements. A third benefit is the primary loop is not pressurized until after the first engine firing. The design of the venturi is not critical because the flow rate in the vaporizer circuit could vary considerably without affecting the operation of the vaporizer or the normal rocket mode operation. This is an asset for several reasons. One of which is that this loop could be added to an early reactor engine test with very little impact on the overall system.

2. Power Conversion System

The design selection for a power conversion system for a single EOS size stage with small nuclear rocket engine will be less than 25 Kw(e). If the electric power requirement is only a few kilowatts, say 5-10 kw(e), the power conversion system could be similar to the NERVA Dual Mode design. As discussed elsewhere in this report, the shaft speed of the turbine-alternator pump would be increased for the smaller machine. Thiophene is a desirable working fluid for smaller machines and is suitable for higher than 800°R turbine inlet temperature. It may be desirable to look at other working fluids with much higher molecular weights to reduce the number of stages in the turbine especially if the design temperature is increased to 1000-1100°R or more.

3. Heat Rejection System

The basic concept suggested for the heat rejection-radiator loops on the NERVA Dual Mode study may apply to a smaller system. The much smaller exterior surface on a single launch EOS engine-run tank could limit the radiator capability hence the size of the electrical system. Increased maximum cycle temperature for example from 750°R to 1000°R would significantly increase radiator capability thus permit a larger electrical power system.

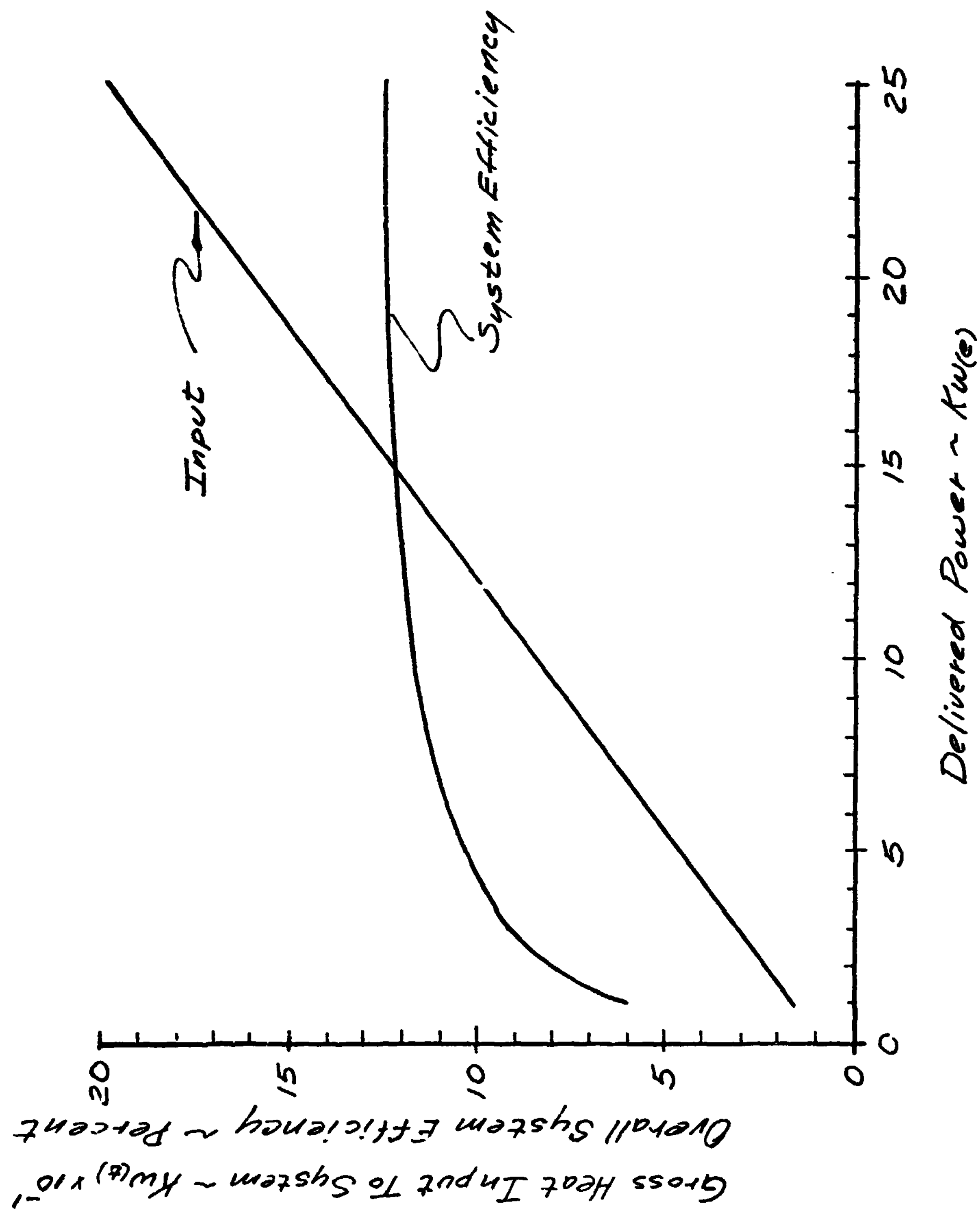
4. Overall System Efficiency as a Function of Power Level

The smaller the design power output the lower the net overall system efficiency which can be expedited. Figure 4 is an estimate of the overall efficiency which may be expected. The figure should be used for initial estimates of heat required and rejected only. The NERVA Dual Mode System had a calculated overall efficiency of 12.5 percent for a power output of 25 Kw(e). This performance decreases to approximately nine percent for a 3 Kw(e) system. If higher maximum temperatures were compatible with the nuclear rocket engine, the efficiency could be increased but the trend would remain.

5. System Weight vs Power Level

As the design power level decreases and the overall cycle efficiency change the component and subsystem weights per unit power change.

Figure 4



Input
System Efficiency
Delivered Power ~ Kw(e)
0
5
10
15
20
25
Overall System Efficiency ~ Percent
Gross Heat Input To System ~ Kw(t) x10-1
0
5
10
15
20

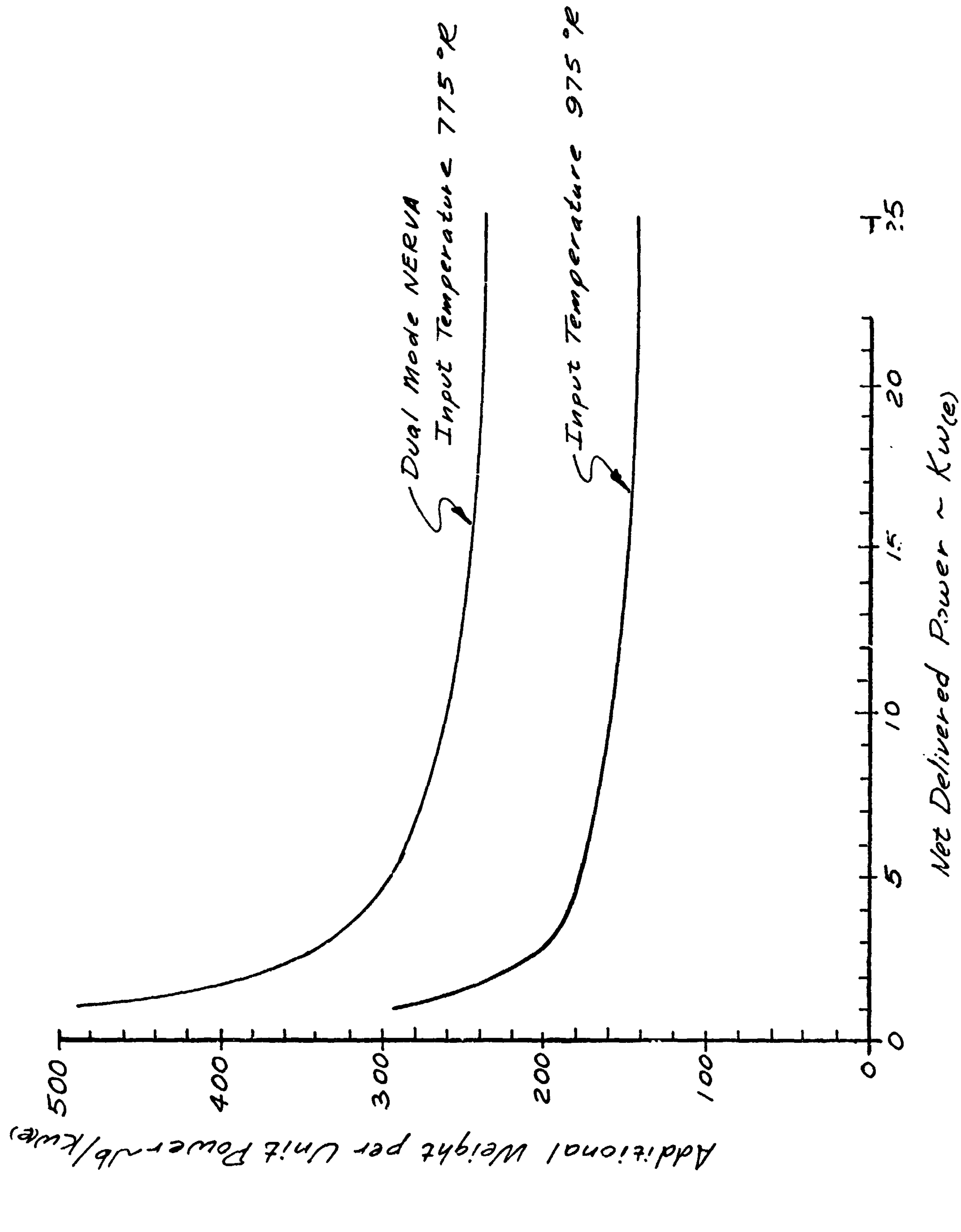
In a low temperature Rankine cycle for space application the heat rejection loop has one-half of the total weight. Clever means (including bookkeeping techniques) may be used to change the radiator weight per square foot, however, the smaller systems will undoubtedly have less output (electrical) per unit heat input hence they will also have less output per unit heat rejected. Based upon the additional weight of 240 lbs per kilowatt of electrical output for the 25 Kw(e) NERVA Dual Mode System and the estimated overall efficiency of this type of system (shown in Figure 4) the variation of estimated additional weight per kilowatt electrical is shown in Figure 5. It will be noted that the weight of very small output systems such as 3 Kw(e) have estimated specific weights of 330 lbs/Kw(e) or 990 lbs additional weight. These specific weights can be improved by increased temperature of the system. A modest increase of approximately 200°R above the NERVA Dual Mode System temperature could reduce specific weight to 0.6 of the lower temperature system. This is also shown in Figure 5.

IV. COMPONENT CHARACTERISTICS

A. The Effect of Design Power Level on Dual Mode Turboalternator and Pump

The Dual Mode Electrical Power Generating System was originally studied (1) at a 25 Kw(e) net electrical power level. A question can be raised as to the validity of the design study if the design power level were to be reduced to one-tenth as much. As the turbine is about 7 inches in diameter and the centrifugal pump less than an inch in diameter, this question is a valid one. Efficiency of the turbine and the pump could be expected to be

Figure 5



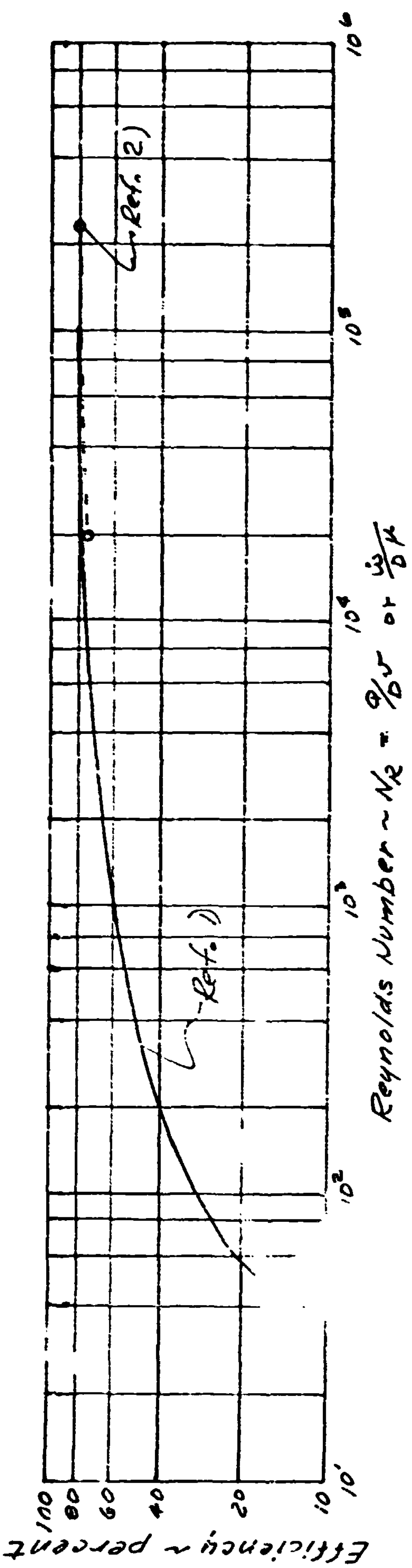
Dual Mode NERVA
Input Temperature 775 °R
Input Temperature 975 °R
Net Delivered Power ~ Kw(e)
Additional Weight per Unit Power ~ lb/Kw(e)
0
5
10
15
20
25
0
100
200
300
400
500

degraded as Reynolds number decrease and clearance ratios increase. This loss of efficiency may be illustrated for decreasing Reynolds numbers and increasing clearance in figures 6 and 7, respectively.

The 25 Kw(e) Dual Mode turbine as proposed in the referenced study, (1), has a First Stage Reynolds number of 113,000 (in w/Dμ form) and 81,500 for the last stage. Examination of Figure 6 illustrates that efficiency is not seriously jeopardized by a decrease in Reynolds number at the 10^5 level. In addition the physical size of the turbine will be decreased as the design power level is reduced. This would result from the desire to keep the turbine specific speed ($NQ^{1/2}/\Delta H^{3/4}$) constant in order to realize the maximum design efficiency possible. Optimum specific speed is around 100 in ($rpm, cfs^{1/2}/ft^{3/4}$) units. As the design power level is reduced, the volume flow rate would be reduced proportionally. Thus, in order to keep design specific speed constant, the design speed should be increased by the square root of the flow rate ratio. Thus design Reynolds number will decrease as the square root of the power ratio rather than directly. Turbine Reynolds numbers would be reduced to 35,800 and 25,800 from 113,000 and 81,500 for a ten to one reduction in design generating power. Expected loss of turbine efficiency would be about 2 percentage points for the 2.5 Kw(e) turbine alternator compared to a 25 Kw(e) design.

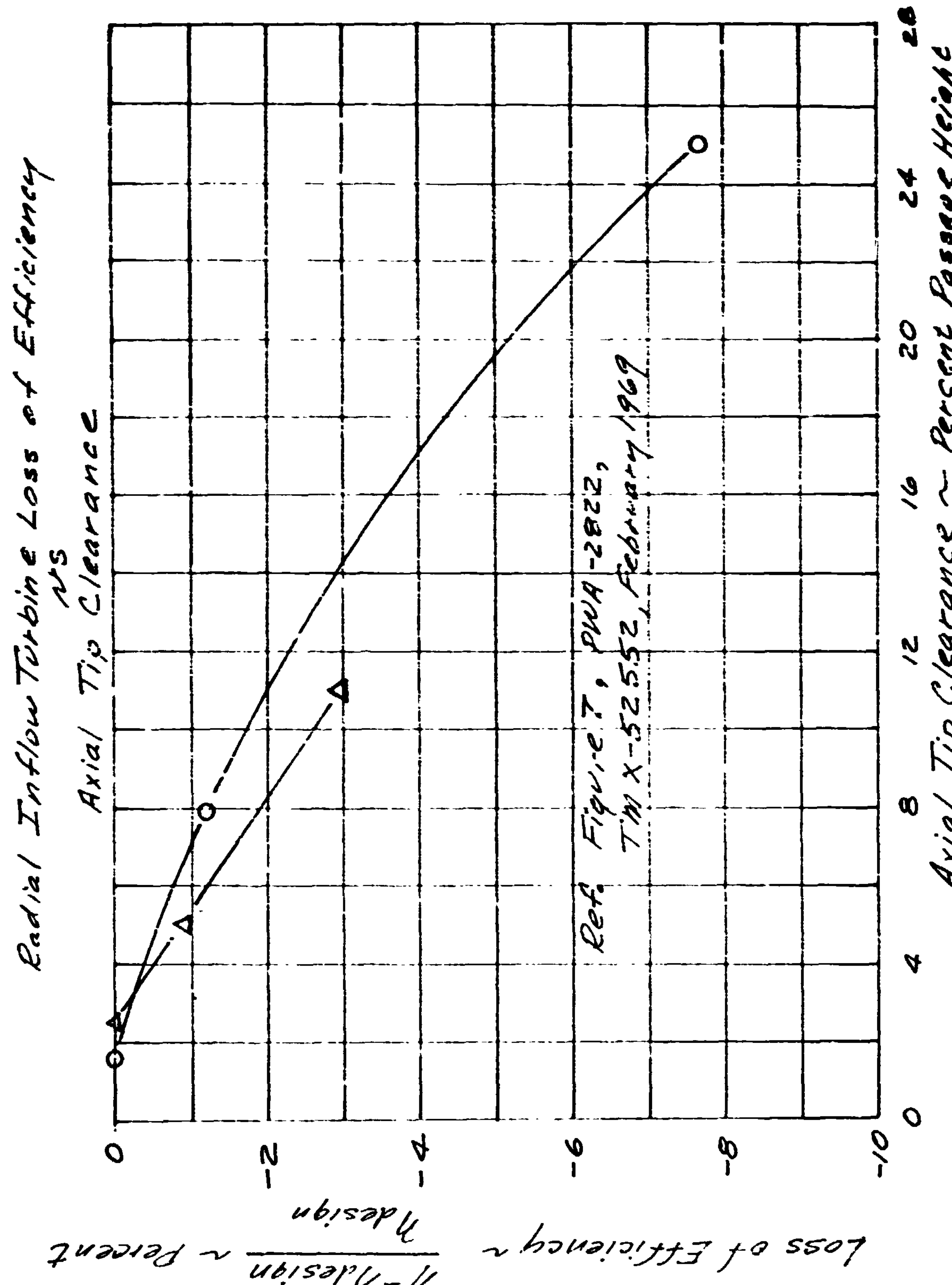
Relative roughness could also effect the performance of small turbomachinery if the passages were cast or the parts extremely small. Passages would have to be on the order of 0.1 inch hydraulic diameter for a 100 μ inch finish to even start to degrade efficiency,(2). Rotors would have to be less than an inch in diameter before relative roughness need be considered.

Figure 6



Ref. 1) Figure 14.17, page 317
Centrifugal and Axial Flow Pumps
A.J. Stepanoff
John Wiley & Sons 1948

2) page 14, *Experimental Performance of a 5 inch Axial Flow Turbine Over a Range of Reynolds Numbers*
S.M. Futral, Jr.
NASA TM X-1679, October 1968


Radial Inflow Turbine Loss of Efficiency
vs
Axial Tip Clearance
Ref. Figure 7, PWA-2822,
TM X-52552, February 1969
Axial Tip Clearance ~ Percent Passage Height
0
4
8
12
16
20
24
28
Loss of Efficiency ~ (η-η design)/η design ~ Percent
0
-2
-4
-6
-8
-10

The centrifugal pump proposed for the 25 Kw(e) power level also has a design specific speed of 100. Without any Reynolds number, clearance or relative roughness effects, it can be designed to reach + 80% efficiencies, (3). Estimated design efficiency used in the study was degraded to 44% to account for Reynolds number, clearance, and cavitation losses. It's design Reynolds number is 58,600. A reduction of Reynolds number to 18,400 could also be expected to reduce the efficiency another 2 percentage points, Figure 6. In view of the conservative design value assumed for the pump, 44 percent, a reduction in power to one-tenth the originally proposed design value is expected to have negligible effect upon the overall turboalternator efficiency.

As turbomachinery is reduced in size the parasitic power consumption of the bearing system can become a larger portion of the input shaft power. This happens when the bearings are not scaled down in proportion to the other parts of the rotating assembly or the residual rotating assembly unbalance cannot be scaled down to the lower sensitivity limit of the balancing machine available.

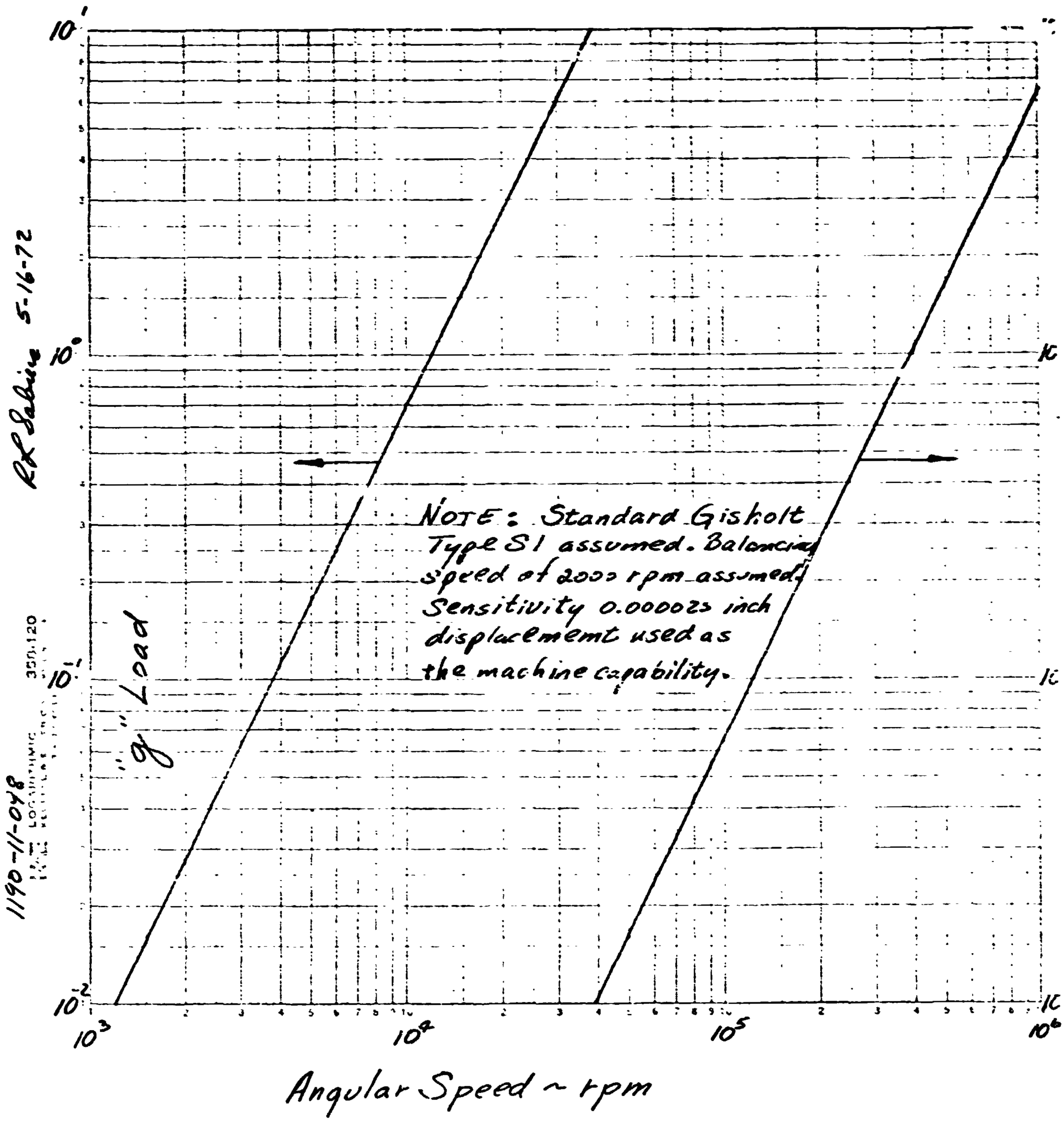
The proposed turboalternator was designed to use hydrodynamic bearings. Rolling contact bearings were considered in the earlier study but were not selected because of the extra complication of the required labrication system and an insufficient bearing life to meet the eight year requirement. Subsequent design studies reconfirm this conclusion, (4). The Rankine fluid (thiophene in the case of the study (1)) was selected as the lubricant to minimize the scaling, contamination, and lubricant deterioration problems.

In scaling down rotating machinery using hydrodynamic bearings we find that the design bearing loads scale differently dependent upon whether the weight of the rotating assembly or its inherent dynamic unbalance is the larger operating bearing load. When weight is the controlling bearing load, the weight (scaling as a function of scale cubed) will be reduced at a greater rate than the bearing support area which is a function of scale squared. As a result of the bearing length and/or diameter can be reduced to a greater degree than the scaling ratio and result in bearing friction power to shaft power ratio being reduced as a machine is scaled down, see bearing discussion, Section IV-C.

If, however, the inherent rotating assembly unbalance is the significant bearing design load, then the parasitic bearing friction to shaft power ratio may stay constant as the machine size is reduced or it may increase if we have reached the practical balancing machine sensitivity limit. To illustrate the balancing machine limitation on bearing loads Figure 8 plots the unbalance force in relative earth gravity units as a function of rotor speed for single plane unbalance. The standard Gisholt S-1 is assumed as the balancer. It typically balances in room atmosphere at rotor speeds of 1000 to 3000 rpm. The plot assumes a 2000 rpm balancing speed and the minimum balancer displacement sensitivity of 0.000025 inch. The plot gives the operating unbalance bearing load as a function of operating speed. For operating speeds up to 10,000 rpm it is noted that the unbalance bearing load would be less than the normal gravity load here on earth. For machines between the steady speed range of 24,000 to 96,000 rpm, it can be seen that the resulting bearing unbalance load reaches undesirable levels. Thus a balance machine having greater sensitivity and/or having higher balancing speed capability will have

Figure 8

BEARING "g" LOAD as a FUNCTION of OPERATING SPEED RESULTING FROM RESIDUAL UNBALANCE

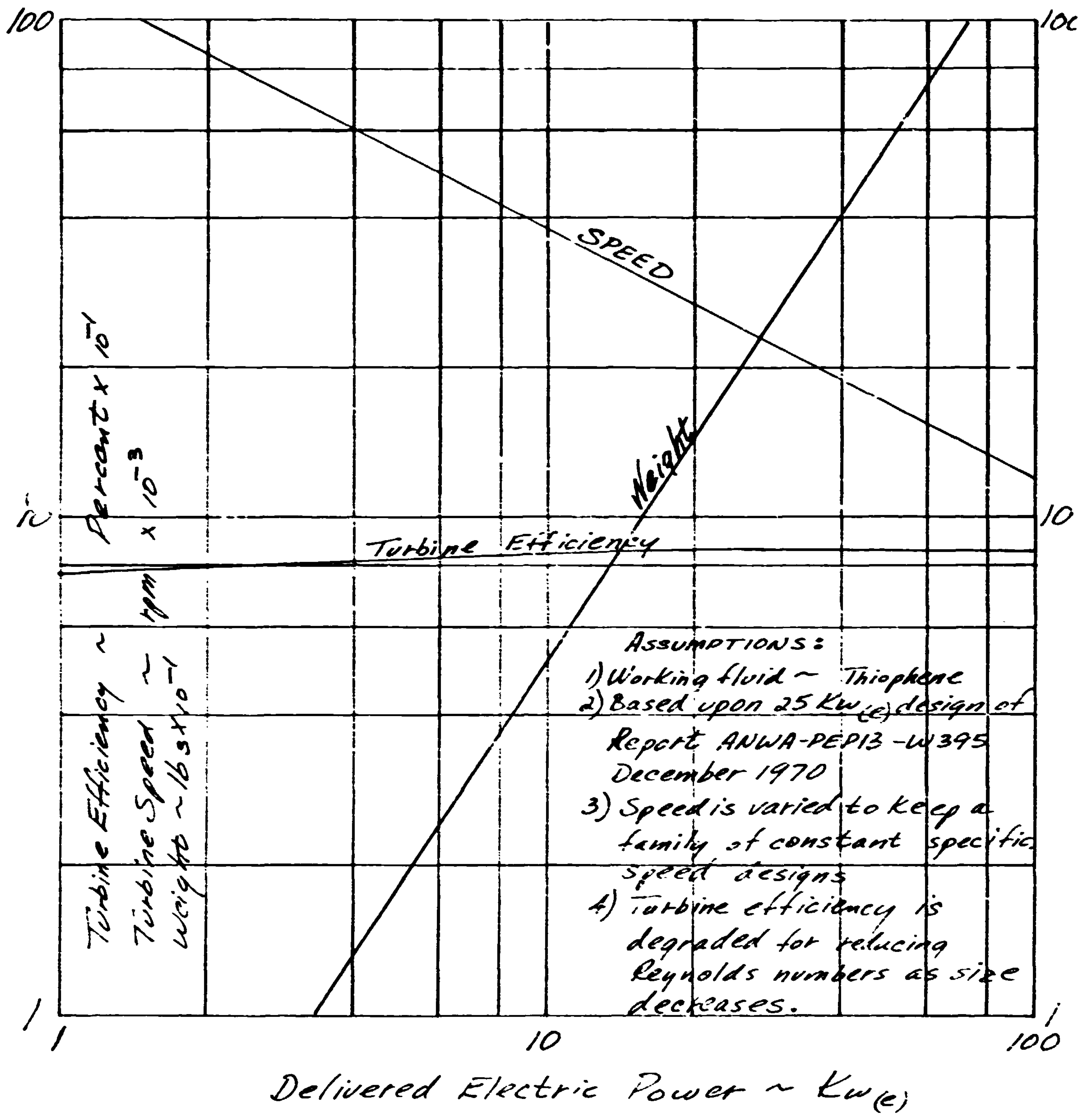


to be utilized for smaller sized turbo-alternators. Commercial balancers are available (5) that will correct balance to less than one "g" at 100,000 rpm. We are thus able to scale bearing design loads and thus bearing friction power for machines up to the 100,000 rpm operating speed. Above that speed either additional cost for balancing will be required or the bearing friction power will not scale down with smaller machine sizes. A turbo-alternator of one-tenth the power would be approximately one-third the physical size of the proposed Dual Mode design. It would want to turn at about three times the speed or 72,000 rpm to stay with a multiple of 400 cycle power. The hydrodynamic bearings will therefore scale down in size and in friction power as a function of the shaft power ratio.

Summarizing, there appears to be no fundamental reason why the proposed Dual Mode turbo-alternator of 25 Kw power level cannot be scaled down to 2.5 Kw. The turbine and pump may suffer but an insignificant efficiency loss provided mechanical features such as leakage and clearance dimensions can can also be scaled down. The hydrodynamic journal bearings may scale down to the one-tenth power level without suffering out of proportion parasitic losses. Other considerations, such as critical speeds, may dictate larger shaft sizes than those required for bearing loads. Thus small machines often incur larger bearing parasitic losses. The magnitude or relative importance of leakage losses and bearing losses is a function of the detail design of the machine. Estimated turbo-alternator weight as a function of developed electrical power for a family of constant Specific Speed machines is shown by Figure 9 to be less than 10 pounds for a 2.5 Kw unit.

Figure 9

Turboalternator Efficiency, Speed, & Weight vs Delivered Power



IV. COMPONENT CHARACTERISTICS (Contd.)

B. Bearing Selection Criteria - Liquid or Gas

The requirement for a high degree of reliability over an eight year life precludes the use of rolling contact bearing systems as noted in Section IV-A. The remaining practical types are the fluid film bearings that have no solid contact and are not subject to fatigue life limits.

There are a number of factors to be considered in narrowing the choice to a specific design. These considerations are controlled by the environment around the bearing and at best will be a compromise to be made during a final design study. We can identify the parameters that must be studied, however.

Primary influence upon the bearing environment is the type of atmosphere, the temperature, and the pressure that surrounds the alternator rotor. As the rotor I^2R losses must be removed by convection cooling, sufficient heat transfer capacity must be allowed the fluid to keep the rotor at acceptable temperatures. This means the type of fluid and its pressure are significant variables. Gas properties that enhance heat transfer are high specific heat, high thermal conductivity, and a low viscosity. Of these parameters, only the density (pressure) is independent of the gas selected. We thus have the pressure and the type of gas to consider in a trade-off consideration.

To enhance reliability it is generally desirable to have machinery with few and relatively simple parts. It was thus suggested (1) that the

Rankine Cycle working fluid be used exclusively for all requirements of fluids in the turboalternator. By using the same fluid in the turbine, bearings, cooling passages, and pump, non-leaking type seals can be avoided and all contamination problems between fluids are eliminated. The Rankine Cycle provides the pressure-temperature regimes that make either gas or liquid available for any of the above functions.

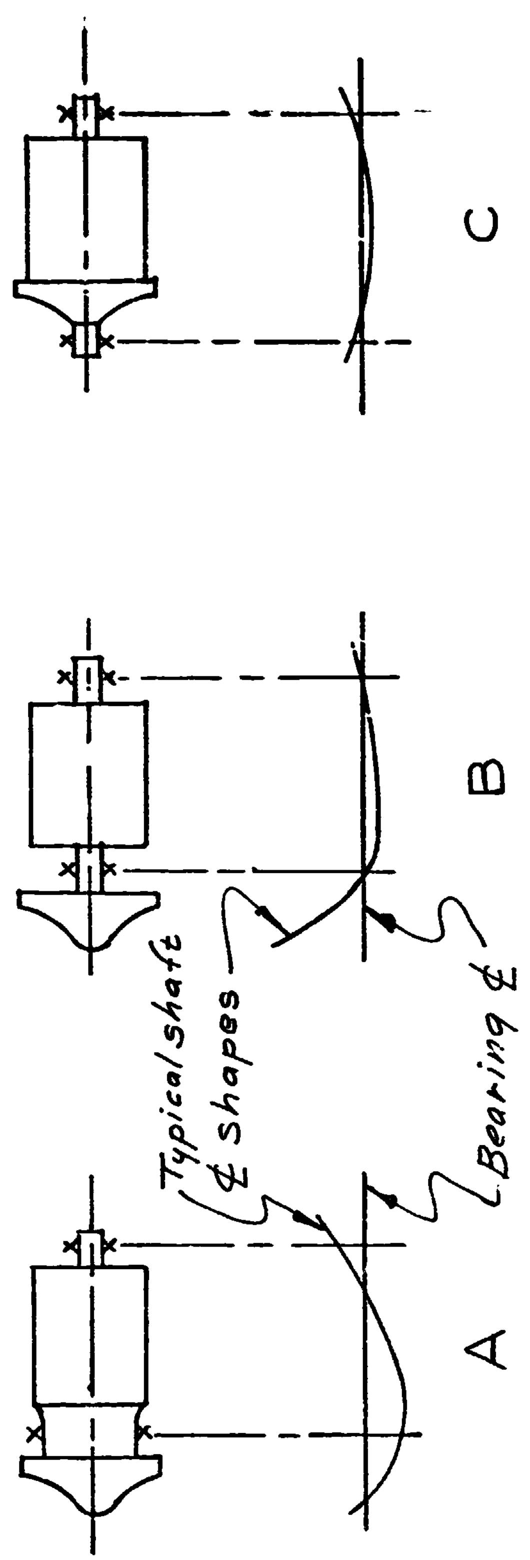
The alternator cooling can most easily be done by vapor in the housing surrounding the rotor and liquid through the stator. Sufficient pressure must be kept in the housing to inhibit corona leaks for the voltage selected. Higher pressures than necessary to suppress corona limitations might enhance cooling as previously noted, however, it will also increase the rotor windage losses. A trade-off between alternator voltage, cooling requirements, and windage losses will have to be made during the detail design period.

It is apparent that there may be various pressures required from one end of the turbo-alternator pump to the other end. Hydrodynamic or viscous type seals would be required to separate these various pressure regimes as no rubbing type seal would have an eight year life. Dependent on the pressure differences, either gas or liquid could be used.

To complete the bearing system trade-off study, the bearing locations, shaft critical speed, and bearing damping rates must be considered along with the determination of whether liquid or gas films should be used. For a given load and speed one would prefer to use liquid film bearings as they would have less parasitic power demand, Section IV.-A. This assumes, of course, that the smaller journal diameters allowed by the liquid film bearings do not create any unacceptable side effects. These could be in the form of too low a shaft bending critical speed or too small a shaft displacement at the bearing location to allow sufficient bearing damping. The larger diameter bearing of View "A" Figure 10 illustrates sufficient displacement while "B" might not. Should shaft critical speed or bearing damping problems arise from the small bearings desired to reduce parasitic power, the turbine and bearing could be placed outboard of the turbine rotor as illustrated in "C" of Figure 10. This configuration should be sufficiently stiff to raise the third bending critical well above the operating speed. Typical liquid bearing designs have sufficient damping to operate at the third shaft critical. That is they are critically damped.

It should be pointed out the viscoseals that might be required can be integrated into the journal bearings and recoup some benefits. By combining the two, bearing and seal, parasitic power could be reduced while achieving a stiffer bearing. In addition, the bearing will be more stable. It has a higher spring rate as the bearing eccentricity ratio approaches zero. This type of bearing-seal has been demonstrated as a canidate configuration for high speed alternates, (6).

Figure 10



Typical Shaft Centerline Deflections

Power Consumption:

The friction parameter of Figure 14 of reference (7) is equal to

$$f = \frac{4\Pi\Phi}{R_e} = 0.02 \text{ @ } R_e = 1795$$

as $R_e = \frac{Uc\rho}{\mu}$

Substituting $\Phi = \frac{fRe}{4\Pi} = \frac{fUc\rho}{4\Pi\mu}$

Substituting Φ in equation 15 of reference:

$$q = \frac{\Phi\mu\Pi DLU^2}{C} = \frac{fDLU^3\rho}{4}$$

$$= \frac{2 \times 10^{-2} \times 4 \times 10^{-2} \times 1.166 \times 10^{6} \times 2.02}{4 \qquad 1.2 \times 10^{1}}$$

$$q = 3.92 \times 10^{1} \ \frac{\text{lb ft}}{\text{sec}}$$

or $\boxed{0.0533 \text{ Kw per inch of seal}}$

In conclusion it appears that liquid hydrodynamic bearing with visco seals in combination as required should receive first consideration as the bearing-sealing system for a Rankine cycle space turbo-alternator-pump assembly. It is simple, (few parts), is not limited by fatigue life, or wear, and is capable of properly supporting the rotating shaft assembly. In addition, the viscoseal can accommodate pressure differentials across the bearings that are greater than those anticipated.

C. Scaling of Hydrodynamic Bearings

Radial bearing loads consist of the forces resulting from acceleration of the vehicle the machinery rides in and the dynamic unbalance forces inherent in all rotating machinery. In both instances these forces are proportional to the mass of the rotating parts. Radial bearing loads due to vehicle or gravity acceleration are proportional to the size of the machine cubed. Thus

Bearing Force (f) Weight (f) Mass (f) $(\text{Scale})^3$

for similar machines of the same materials. When the bearing diameter is used as the characteristic to scale then the bearing load is:

$$F_B \text{ (f) } d^3$$

As machines become smaller and the angular speed increases then the dynamic unbalance force can be predominate over the rotor weight. If it is assumed that unbalance force is proportional to size then

$$\bar{r} \text{ (f) } d$$

and the bearing load is then portional to scale or diameter squared:

$$F_{BU} = \frac{W}{g} \bar{r} \omega^2 \text{ (f) } \frac{d^3}{g} d \omega^2 \text{(f) } d^4 \omega^2$$

and as it is assumed that linear velocity will be constant for a series of constant specific speed machines, that is:

$$u = r\omega = \text{constant and}$$

then the bearing load due to unbalance forces is:

$$F_{BU} \text{ (f) } d^2$$

If the unbalance torque does not scale down with size, then $\bar{r}$ is constant and

$$F_{BU}\text{(f)}\cdot d$$

We thus have the possibility that the radial bearing load will scale as d^3, d^2, or d depending on the design circumstances.

Hydrodynamic bearing capacity is a function of viscosity, bearing linear velocity and bearing length, equation (5-90) of Reference (8)

$$W_{BC} = \mu UL \frac{r^2}{C^2} \quad \frac{12\Pi n}{(2 + n^2) \sqrt{1-n^2}}$$

If the machine is scaled proportionally then bearing length to diameter ratio is constant. The bearing length in the above capacity could be replaced by the diameter.

W_{BC} (f) d only if relative clearance ratio is kept, $\frac{c}{r}$, eccentricity ratio, n, and the linear velocity, U, are all constant.

As the bearing capacity, W_{BC}, must equal or exceed the design load, F_{BU} or F_B, it is interesting to note that when scaling down a given design the

the bearing capacity equals or exceeds the imposed load. The cases where the imposed load varies as d^3 or d^2 will allow a bearing designed for a smaller L/d ratio or a length L, that is smaller portionally than the scaling ratio.

Looking at the bearing friction power we find that

$$HP_f \quad (f) \; \frac{U\, r\, r^2 L N^2}{C} \; (f) \; \mu L U^2$$

where the clearance ratio, c/r, is constant. For a family of geometry similar machines

HP_f (f) L or (f) d if L/d is constant.

The bearing parasitic losses will scale directly with the linear size for a constant L/d ratio designs. As the imposed loads can vary from the scaling ratio cubed to linearily, the parasitic losses can scale directly or as a function of the cube power.

Thus

HP_f (f) d for constant L/d ratio bearings.

But as the shaft power of a turbomachine varies with the square of size:

HP_s (f) d^2

then the parasitic bearing power will become a larger percentage of the shaft power as a machine is scaled down to smaller power levels.

We thus have the following possibilities:

(1) If the unbalance force cannot be scaled down with size and it produces the significant bearing design load, then parasitic bearing power will increase as a percentage of shaft as a given design is scaled down.

(2) If the unbalanced force can be scaled with size and it produces the significant design load then parasitic bearing power scales directly with shaft power.

(3) If the normal earth gravity or vehicle acceleration loads are the significant bearing design loads then the design load scales down faster than the power. In this case the bearing length, L, could be reduced by more than the scale factor and thus the parasitic load as a percentage of the shaft power will reduce as the machine is scaled to a smaller size.

D. Relative Power Consumption of Gas and Liquid Film Bearings

It is desired to determine if a gas or liquid hydrodynamic journal bearing will have the larger friction power if both are designed for the same loading and same speed.

Taking the following assumptions from the original Dual Mode Study (1):

Fluid - Thiophene

Temperature - 100°F

Bearing type - radial hydrodynamic

Speed - N rpm

Load - W - lbs

Journal diameter - not to be limited by shafting critical speed

Eccentricity ratio - n - constant

Clearance to radius ratio - $\frac{C}{Y}$ - constant

Viscosity @ 100°F

Liquid - $38 \times 10^{-5} \frac{\text{lb}}{\text{ft sec}}$

Gas - $1.3 \times 10^{-5} \frac{\text{lb}}{\text{ft sec}}$

The journal bearing load is:

$$W = \mu UL \left(\frac{r^2}{C^2}\right) \frac{12\pi n}{(2 + n^2)\sqrt{1 - n^2}}$$

Ref: equation 5-90 page 161 of Reference (8).

For bearings of equal capacity, W, $\frac{C}{r}$ ratio, and eccentricity ratio, n, where μ is viscosity, U is the journal interface velocity.

For constant length, L;

$$W \text{ (f) } \mu U = \mu dN$$

thus $d \text{ (f) } \frac{W}{\mu}$ for constant speed, N,

and for a constant $\frac{L}{d}$ ratio and speed, N.

$$d \text{ (f) } \sqrt{\frac{W}{\mu}}$$

Bearing friction power is:

HP_f (f) TN (f) FdN where T is friction torque.

Bearing friction force is:

$$F = \frac{\mu U L r}{C} \quad \frac{4\pi\,(1 + 2n^2)}{(2 + n^2)\sqrt{(1 - n^2)}} \qquad \frac{lb}{ft^2}$$

Ref: equation 5-97, page 182 of

Reference (8).

Now for constant, C/r ratio, eccentricity ratio, n, and speed, N;

F (f) μdNL

and for a constant length, L and speed, N;

HP (f) FdN (f) μd^2L (f) μd^2

and for a constant $\frac{L}{d}$ ratio

F (f) μd^2n

HP (f) FdN (f) μd^3 for

constant speed.

Friction power ratio for the two cases will be related by:

$$\frac{(HP_f)\text{gas}}{(HP_f)\text{ liquid}} = \frac{\mu_g d_g^2}{\mu_l d_l^2} \text{ for constant L}$$

and

$$\frac{(HP)\text{ gas}}{(HP_f)\text{ liquid}} = \frac{\mu_g d_g^3}{\mu_l d_l^3} \text{ for constant L/d ratio.}$$

Substituting the journal diameters as a function of the load requirements:

$$\frac{(HP_f)\ \text{gas}}{(HP_f)\ \text{liquid}} = \frac{\mu_g}{\mu_l}\left|\frac{\mu_l}{\mu_g}\right|^2 = \frac{\mu}{\mu_g} \quad \text{and}$$

for constant $\frac{L}{d}$ ratio substituting for the journal diameters:

$$\frac{(HP_f)\ \text{gas}}{(HP_f)\ \text{liquid}} = \frac{\mu_g}{\mu_l}\left|\frac{\mu_l}{\mu_g}\right|^{\frac{3}{2}} = \sqrt{\frac{\mu_e}{\mu_g}}$$

In conclusion it is apparent that a gas bearing designed for the same speed and load will develop more friction power than a liquid bearing regardless of the design criteria used. For the assumed case of thiophene at 100°F:

the gas bearing of constant width, L, design will have

$$\frac{\mu_e}{\mu_g} = \frac{38.2 \times 10^{-5}}{1.3 \times 10^{-5}} \qquad \text{30 times the}$$

power consumption of the liquid bearing and the constant L/d design will have

$$\frac{38.2 \times 10^{-5}}{1.3 \times 10^{-5}} \doteq \text{5 times the parasitic power.}$$

E. Rankine Cycle-Turbine

The temperature limitations of the NERVA engine made the Rankine cycle the most desirable choice for the dual mode power system. Long life favored the selection of a turbine over a reciprocating or rotory expander. To avoid erosion in the multiple stage turbine, the working fluid selected (thiophene) has a dry expansion even though the initial state point is only saturated vapor. Also, the near vertical slope of the saturated vapor line (on the temperature-entropy diagram) minimizes or eliminates the need for a heat exchanger at the turbine outlet to desuperheat the vapors prior to their flowing into the condenser. This additional heat exchanger (regenerator) is generally required for maximum cycle efficiency with working fluids which have very large molecular weights.

The nominal vapor pressure of the Rankine cycle working fluid at the maximum cycle temperature greatly eases the boiler (vaporizer) feed pump mechanical design problems. A small, single stage centrifugal pump on the turbo-alternator shaft was entirely adequate for the NERVA Dual Mode System design. A significant increase in maximum cycle temperature coupled with a reduction of flow rate (reduced electric power output) could make the boiler feed pump design more difficult. If this situation should arise, attention should be directed at the pitot strut centrifugal pump. The jet pump will still be necessary to insure reliable suction flow from the condenser in a zero gravity environment.

F. Turbine-Alternator Efficiency

In a Rankine power conversion system operating in the temperature ranges of interest for dual mode nuclear rockets, the turbine x alternator efficiency product is the single design factor of most importance to the overall system performance. This is true because the modest temperature differentials of the low temperature Rankine cycle provides theoretical diagram efficiency close to the ideal Carnot efficiency. Thus the turbine and alternator efficiency directly affects the output shaft and electrical power compared with the ideal power available from the cycle. To add to the importance of the turbine-alternator efficiency is the fact that for small systems the parasitic power is a large fraction of the gross output. Hence, a significant portion of the turbine-alternator output is consumed as a tare. A small increase in turbine efficiency increases the gross output directly in proportion to the increase in turbine efficiency. This is reflected in a even greater than directly increase in the net shaft or electrical output because of the relatively large tare.

In general, the weight of the turbine-alternator is small in comparison with the weight of the vaporizer or condenser-radiator. As a consequence, no effort should be spared in the design of the turbine-alternator to enhance its efficiency. The turbine should have a specific speed and number of stages which give maximum overall efficiency. The highest turbine efficiency will result in the lowest heat flow into the dual mode power system per unit of electric power and an even greater reduction of heat which must be rejected through the enormous radiator system.

The alternator must also function as a motor to drive the boiler feed pump when starting up the Rankine power conversion system. This same feature could be very useful for operation of the Rankine cycle as a dynamic heat pipe during the early portion of cooldown when it may be desirable to temporarily operate the radiator at higher heat flux and temperature to conserve cooldown fluids or for earlier thrust termination without increasing the temperature of the primary loop coolants.

When the alternator is operated as a motor, the electrical energy must come from an outside source such as a battery-inverter supply. The electric power desired is relatively small because the feed pump power is small and the turbine drag is also small.

G. Off Design Operation During Cooldown

The Dual Mode NERVA electrical power system was constrained by the temperature capability of aluminum components in the nuclear rocket engine. This 800°R maximum cycle temperature resulted in the temperature selection of 530-540°R for the radiator. For missions wherein multiple burns were required, the heat rejector capability of the radiator was useful as a device to reject decay heat from the engine during cooldown thus reducing the quantity of cooldown hydrogen required and the duration of thrusting associated with cooldown hydrogen flow. The savings in cooldown fluids could be increased by increasing the radiator capacity i.e., more radiator area or increased radiator temperature. The later scheme was found to be quite attractive for the period of cooldown only. An increase of approximately a factor of three was within scope by increasing the heat rate from the engine and allowing the radiator temperature to rise from 530°R up to 660°R. For this period the electrical output from the power conversion system was zero. Special provision in the design of the Rankine cycle are necessary to accomplish the increased heat flow rate. The major addition component is a turbine nozzle bypass flow duct. In a sense the Rankine cycle becomes a dynamic heat pipe for this off-design mode of operation.

The off-design mode of operation described above could be accommodated because the materials of construction for the tanks and radiator system could stand the temperature increase form 530°R up to 660°R for the cooldown period. If the radiator design were at its maximum temperature for normal electric power generation mode no margin for increase temperature for cooldown would be available.

H. <u>High Temperature Radiator</u>

Many space electric power systems which have been studied and funded for development have been based upon use of a high temperature heat rejection surface (radiator). The SNAP-8 Mercury Rankine system is an example of this type of system. The advantage of using a radiator with a high temperature (800-1200°R) lies in greatly reduced surface area per unit of heat rejected. The size reduction may also permit a weight reduction too.

Unfortunately higher temperature radiators may be difficult to integrate with other components on a space vehicle. For example, a low temperature radiator may be incorporated around the exterior surface of an insulated (plastic foam) aluminum propellant tank whereas higher temperature radiators must be physically separated from other vehicle surfaces. This requirement implies that the high temperature radiator surfaces must be extended from the basic vehicle. Both sides of an extendible surface may be utilized for radiating surface which further reduces the plan form area and potentially the weight. However, the weight and structure associated with the extendible surfaces must be included as a part of the radiator system weight.

High temperature radiators have not been studied in conjunction with the dual mode NERVA space electric power system because the temperature of the primary loop was limited by the construction details of the nuclear rocket engine to values compatible with aluminum alloy. As a consequence 660°R was the maximum temperature considered for the radiator.

I. Low Specific Speed Fans

Multiple independent radiator loops require individual electric motor driven circulating fans hermetically sealed in each of the loops. Division flow circuits reduces the volume flow rate in each loop and therefore tends to reduce the fan(s) specific speed to relatively low values even for the 25 Kw(e) NERVA Dual Mode design. Design of the piping system in each of the coolant circuits and selection of high speed motors will help keep the fan specific speed in a desirable range without resorting to multiple stages. For smaller electric power systems it will probably be desirable to reduce the number of independent radiator flow circuits from the 24 selected for the NERVA Dual Mode System design.

J. Coolant for Primary and Radiator Loops

The selection of the type of fluid (i.e., liquid vs gas) used in the loops of the dual mode system is of interest because the selections are different from those normally made for space electric power systems. The coolant selected for the primary loop was gaseous hydrogen. Major considerations in this selection were that hydrogen is used for the rocket mode thus there is no compatibility problem within the reactor and the readily available supply of hydrogen onboard a nuclear rocket stage. Although gaseous hydrogen is an excellent coolant it is necessary to maintain a nominal pressure (500 psia) to keep pumping power within reasonable limits. Other good features of hydrogen are its non-freezing and non-activation by neutrons. Potentially adverse characteristics of hydrogen are leakage through valves and gasketed joints and embrittlement of metals. Design accommodations for these adverse characteristics can be provided for if they are recognized early.

The waste heat rejection loops also utilize gaseous hydrogen for coolant. Hydrogen has an additional advantage over many other coolants especially liquids in the application because its low density reduces the weight of the coolant inventory.

Hydrogen leakage from the multiple heat rejection loops can be expected. The exposure of the radiator tubes to meteorites is inherent in a space radiator design. Make-up hydrogen gas is a possible method of maintain system pressure provided the leak rate is extremely small.

The multiple EOS launch technique with assembly of a space vehicle in orbit entails special problems for the heat rejection system if gaseous hydrogen is circulated across the in-space assembled units. This problem could be overcome with a heat-coupler based upon the heat pipe principle which would not use flowing hydrogen across the interface between stages.

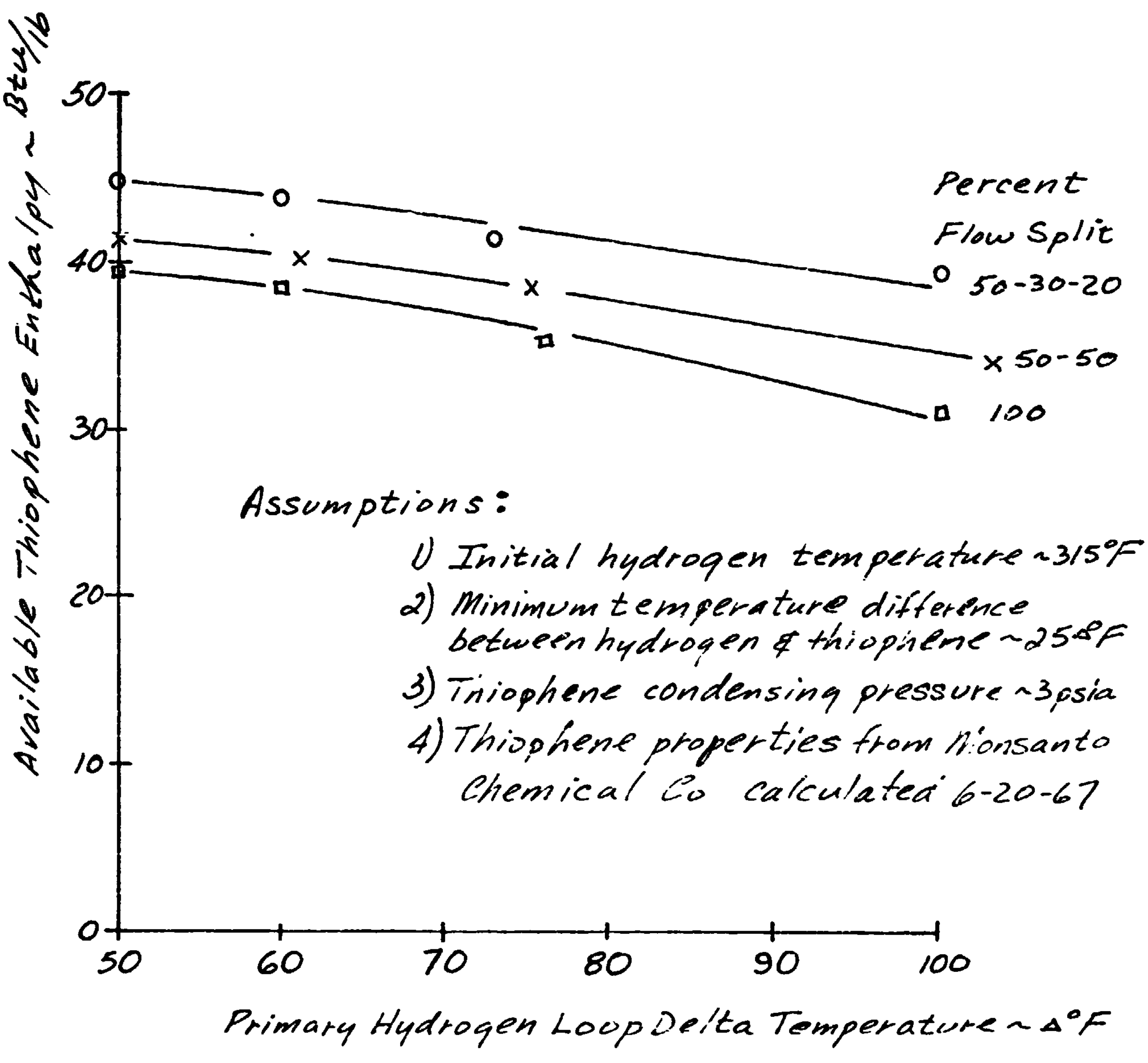
K. Boiler-Vaporizer Design Point Influences

The significant influence of loop delta temperature and multiple pressure level boiling was identified during the original Dual Mode Electrical System Study (1). In reviewing the design at smaller power levels, these influences were ascertained for thiophene at the original design conditions of NERVA Dual Mode Electric system. Three variables of a Rankine Cycle that should be studied during the design point selection are (1) the primary or source fluid delta temperature (2) Rankine Cycle fluid flow ratios used for multiple pressure level boiling and (3) the thermodynamic fluid.

To obtain the influence factors for these parameters the heating source in the Dual Mode System is a hydrogen stream that has a loop delta temperatures ranging from 50 to 100°ΔF. Results of these calculations are plotted on Figure 11. A drop in availability of 15 to 20% is experienced for all flow ratios as the primary heat loop delta temperature is increased from 50°Δ to 100°ΔF. This implies that a tr -off study of the primary loop pumping power vs Rankine fluid unit availability should be made in order to select the optimum primary loop delta temperature.

The benefits of a multipressure boiler can be of a 15 to 20% increase in Rankine fluid unit availability for all loop delta temperatures considered. The available enthalpy per pound of Rankine cycle fluid (thiophene) was calculated for flow split ratios of 100%, 50-50%, and 50, 30, 20%. These benefits should also be traded off against the additional complexity. In the case of the thiophene system studied there was no additional complexity as the relative flows and differential pressures between the various pressure levels fitted very well to a multi-staged radial flow turbine design.

AVAILABLE ENERGY OF A THIOPHENE EXPANSION
vs
PRIMARY HYDROGEN LOOP DELTA TEMPERATURE
for
VARIOUS THIOPHENE FLOW SPLIT RATIOS



The last variable selection, i.e., what fluid to use in the Rankine loop, is not obvious. Thiophene was selected for the Dual Mode Electrical System primarily on its good thermal efficiency when operating between the required source-sink temperature extremes. The beneficial property in this respect is that the saturated vapor line does have a negative slope on a temperature-entropy diagram as the condensing temperature is approached. As other source-sink temperatures are considered, fluids other than thiophene should be considered.

V. SPECIAL PROBLEMS OF DUAL MODE

A. Reliability - Safety

A detailed investigation of the effect of the dual mode system on overall stage reliability has not been done. It is to be expected that several methods of producing electrical power may be appropriate for a single vehicle. The dual mode electrical power generator system could be the prime or a backup power system.

Consideration was given to the Dual Mode NERVA System from the standpoint of increased safety. One of the major potential failure modes which has safety implications is the loss of coolant (propellant) during rocket mode operation. The dual mode heat transfer loop is an alternative method for removal of decay heat. Unfortunately, the decay heat rates following emergency shutdown cannot be rejected by the dual mode radiative rejection system. An alternative is to use the electr power t ive an emergency coolant pump. Even this approach did not appear to be attractive because pumping power required was too great initially. If an alternative emergency coolant such as ammonia were used, an electrically driven emergency pump could be interesting.

B. Radiation of the Payload

The NERVA Dual Mode concept was incorporated in a system which had a shadow shield in the engine and the aft end of the propellant tank was a small angle cone which was protected from radiation of the engine by the shadow shield, Figure 2. For vehicles which have length constraints rather than minimum weight constraints it may be necessary to use a tank configuration with a "fat bottom". Such a tank design will scatter nuclear radiation from the engine forward to the payload. When the tank is full of propellant, the hydrogen attenuates the radiation. A partially full tank will be more severe operating condition. When the tank is nearly empty of liquid, radiation scattering to the payload can be very significant. After rocket engine shutdown and cooldown irradiation of the payload continues during dual mode operation.

These design features are important to the dual mode concept because more scatter radiation will reach the payload with dual mode operation than without. In the NERVA system, the shield was effective because the tank did not protrude beyond the shadow cone. Shadow shields for "fat bottom" tanks are too heavy to be practical.

A separate annular tank which is jetisoned after depletion of its propellant might be a practical way of utilizing the large radius volume just above the engine. From the above discussion it is evident that a length constraint is very unfavorable to a nuclear rocket stage and to the dual mode concept when the payload is sensitive to radiation. For some payloads such as disposal of radioactive wastes the added radiation from the nuclear engine would not be a problem.

C. Reactor Primary Loop Heat Exchanger

Several configurations of heat exchangers for the reactor primary loop were examined by WANL. The first considered was to use the down-and-up coaxial support tubes. One of the problems encountered was that the downflow through the center tube was heated up nicely but during the return flow up the annulus passage the primary coolant lost heat to the downflow. Thus the outlet temperature of the primary coolant was considerably less than the maximum temperature which occurred near the turn around end. This situation results from good heat transfer across the wall separating the cold downflow from the warmer upflow. Space limitations prevented introducing insulation between these two counter flowing strains.

This same problem was found in the heat exchanger design utilizing holes in the aluminum barrel, Figure 2. However, in this design the hole size could be adjusted to give low heat transfer rates (coefficient) to the incoming flow thus maintaining large film temperature differences at the inlet end, hence not chilling the aluminum barrel. In this way the bulk coolant was heated gradually over the entire length of the counter flow coolant channel and pressure drop was small. The only limitation was the loss of material due to the numerous holes in the aluminum barrel.

In the small engine being considered at LASL the counter flow tie tube circuit appears to be most promising for the reactor primary loop heat exchanger. Zirconium hydride moderator in the form of a liner separates the downflow fluid from the upflow fluid. This additional thermal decoupling should ease the counterflow heat exchange effect noticed in the NERVA Dual Mode design.

D. Radiator Orientation

When one thinks of a power plant for ground application, usually the source of energy (fuel) and the power conversion system (engine) are the major items of concern. The heat rejection system is not of major significance. More recently, the problems of heat rejection have become a problem for electrical power generator stations because the waste heat dumped into the rivers has increased river water temperature and the ecologists have become alarmed.

In space, the heat rejection system is of major concern. All heat must be rejected by radiation. Direct sunshine and reflected sunlight from close-by bodies such as Earth or Moon can have a significant heat input to relatively cold radiator surfaces. Thus, it will be clear that the mode of operation can have a significant effect on the design of the space power system. In the case of the NERVA Dual Mode system, the maximum temperature of the heat source was approximately 800°R. Thus the temperature of the heat rejection system was 500-550°R or approximately ambient temperature on Earth which is approximately the temperature of a space radiator in full view of the sun. Under those conditions, the sections of the dual mode radiator would be only partially effective. For this reason conservative radiator area requirements were specified. A change in the attitude orientation of the space radiator so that it is parallel to the rays of the sun and/or an increase in altitude of orbit have a profound effect on the low temperature radiator capability and therefore the system electrical output.

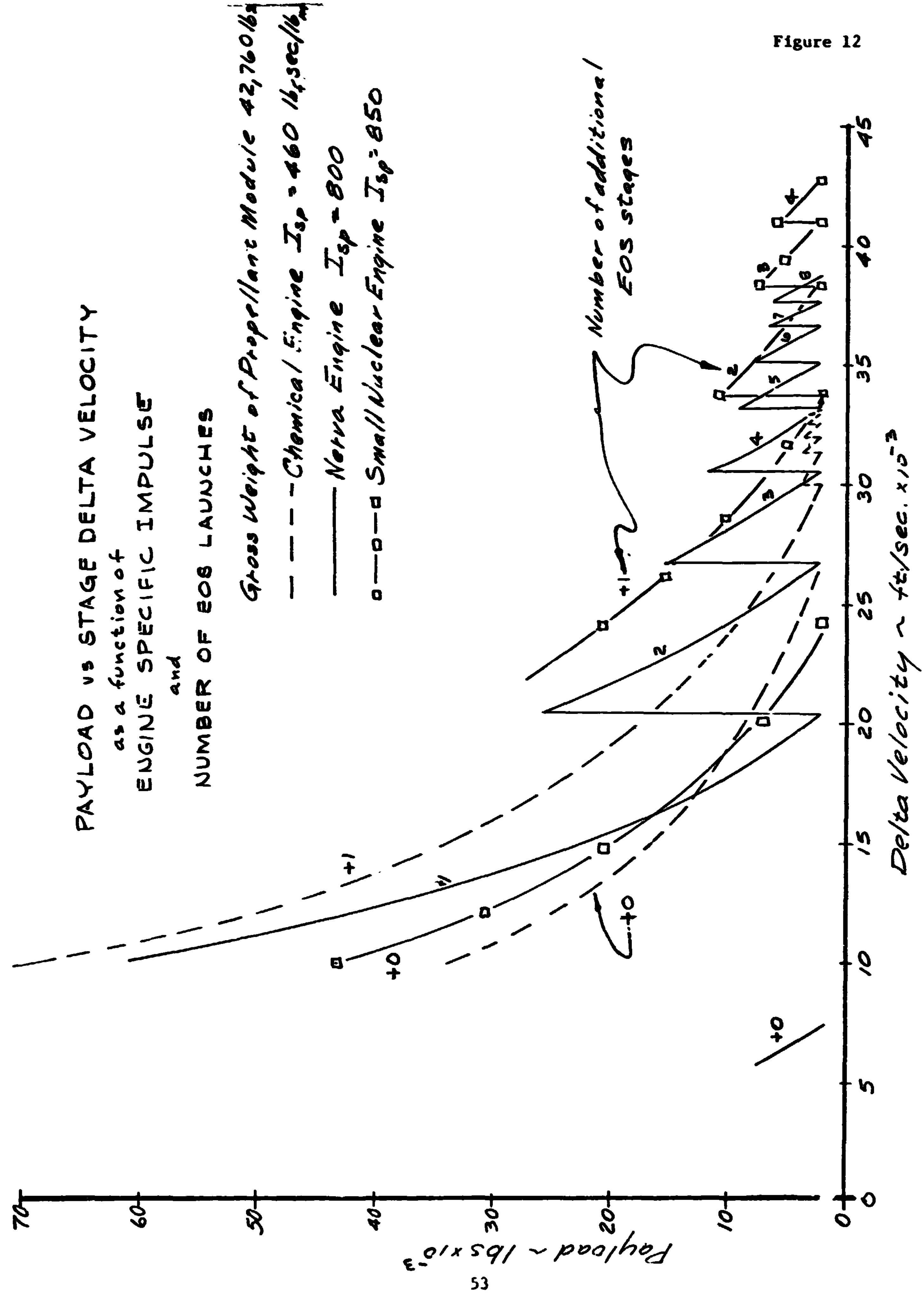
The foregoing suggests a design criteria for dual mode electrical systems which requires that the vehicle be orientated so that the radiator is oriented nearly parallel to the rays of the sun when close to the sun (1 A.U.) and that low altitude orbit (100-300 N.M.) operation utilize an energy storage system to avoid the need for maximum electric power as the stage passes through "high noon".

Orientation of the space vehicle with its radiator parallel to the sun's rays will require vehicle altitude control capability. During coast phase the thrust requirements for altitude control are small, maybe the electric power system can supply the energy for thrusting.

The desirability of orientation of the space radiator is most accute for a low or ambient temperature radiator. If for the small nuclear rocket engine dual mode system (or any other) the maximum cycle temperature is greater and the heat rejection temperature is increased, the impact of other thermal radiation sources on the vehicle heat rejection capability is greatly reduced.

Another approach to reducing the effect of other thermal radiation sources is to use surfaces which have a high coefficient of reflectivity and also have a high coefficient of emissivity at the temperature of the radiator. Research in this field is continuing to increase the durability of surfaces which exhibit these unique properties. These materials can be applied to the control of the temperature of satellites.

Figure 12



PAYLOAD vs STAGE DELTA VELOCITY
as a function of
ENGINE SPECIFIC IMPULSE
and
NUMBER OF EOS LAUNCHES
Gross Weight of Propellant Module 42,760 lbs
Chemical Engine Isp = 460 lbf sec/lbm
Nerva Engine Isp = 800
Small Nuclear Engine Isp = 850
Number of additional EOS stages
Payload ~ lbs x10^-3
Delta Velocity ~ ft/sec. x10^-3
0
10
20
30
40
50
60
70
5
15
25
35
45
+0
+1
2
3
4
5
6
7
8

VI. DEVELOPMENT PLAN

Thoughts given to a development plan for the NERVA Dual Mode System have been reoriented to more closely fit a dual mode system for a small nuclear rocket engine (15,000 lb thrust) with run tank all configured to fit in the 15 foot diameter by 60 foot long cargo bay proposed for the Earth Orbit Shuttle.

Preliminary mission analyses comparing the payload capability of the NERVA, the small nuclear engine and chemical engine stages for various numbers of Earth Orbit Shuttle (EOS) propellant modules are shown in Figure 12. It is important to the dual mode concept to notice that the small nuclear rocket engine (weight 7000 lb) with run tank (labeled "0" propellant modules) has a very significant payload capability for high velocity (deep space) applications. This fact leads to the thought that a basic dual mode system should be incorporated in this basic vehicle. The power producing capability of the basic vehicle will be limited to the range of 3-10 Kw(e) due to radiator surface area limitations (1500-2000 square feet). Additional radiator area can be obtained by extendable surfaces which fold out from the run tank or by using the exterior surface of additional propellant modules (about 3000 sq. ft. each) if they are available.

The development of a dual mode nuclear rocket engine is going to entail more effort than a nuclear rocket engine without dual mode capability. However, a nuclear stage with a dual mode nuclear rocket engine may be significantly less effort than a conventional nuclear stage which has the same features provided by other means. The improved cooldown and early thrust termination feature combined enhance the performance and operation of the nuclear engine while the nuclear energy source of heat during nonthrusting mode is unique for space vehicle applications.

To minimize the impact of the dual mode feature on the development of the nuclear rocket engine, it is desirable to provide for the dual mode feature in the engine design criteria and follow through. In the case of the NERVA, there was no official recognition of dual mode in the NERVA Program Requirements Document (NPRD) although there were requirements for early thrust termination which could be met by a dual mode system and no other satisfactory system had been identified. Also, the engine required a significant guarantee of electricity for normal operations, however, the NPRD engine was not designed to provide its own electrical power.

Fortunately, during the dual mode studies a completely separate and independent flow circuit was identified which could provide sufficient heat to a primary loop coolant to generate 10-25 Kw(e). Since this primary loop flow circuit was independent from the main engine flow circuit, very little interaction occurred. This was very desirable because additional valves and leak tight joints in the main engine flow circuit would have presented serious development problems.

In the case of the small nuclear engine that LASL is considering, it is not desirable to use the reflector region primary loop heat exchanger envisioned for NERVA. The smaller engine may be able to utilize the metal core support structural members for the primary loop heat exchanger and valve off the remainder of the engine flow circuit during non engine firing dual mode operation. The primary coolant circuit would not be "valved-off", it would be permanently piped into the engine flow circuit. The flow circuit diagram is shown in Figure 3.

With the above system in mind it is apparent that the primary coolant loop is already incorporated into the engine design and no significant piping changes are associated with including the lines and tees for the vaporizer circuit. This primary coolant circuit is essentially in parallel with the tie tube flow circuit during normal engine operation and in series with the tube circuit during normal dual mode operation. The venturi concept is introduced to "control" the amount of flow through the vaporizer during engine firing and the heat exchanger removes the heat from the vaporizer flow ahead of the venturi.

The essential point is that the piping changes are outside the pressure vessel and are minimal in scope. The major requirement inside the pressure vessel is that the tie tube circuit be designed and fabricated and assembled as a leak tight system. This additional burden should be undertaken in the early phases of the design for success. Another factor which will be present for a long time to come is the unknown amount of electrical power which is needed. Radiator area on a single run tank will limit the heat rejection capability to the range from 40-150 Kw(t) depending on the temperature, emissivity and environment. With this heat rejection capability the heat input desired will be in the range of 50-200 Kw(t). The support system circuit should be able to accommodate this heat rate readily. Thus, there may not be any great need for very high temperatures in the primary coolant circuit. Low temperature operation can enhance lifetime or durability and keep development costs low.

The dual mode system involves hardware which adds weight to the engine stage. The greater the size of the radiator or heat rejection system the more weight added to the vehicle. However, the larger heat rejection system also permits greater cooldown savings and greater electrical power generating capability. For the NERVA engine studies the engine weight was on the order of 26,000 lbs while the additional weight of the dual mode system for 25 Kw(e) was approximately 6,000 lbs. For lunar missions the payload was approximately 150,000 lbs. When the weight of payload is large compared to the engine weight and the engine weight is large compared with the additional hardware weight of the dual mode system, the additional weight of the dual mode feature is not critical.

For proposed deep space missions the payloads are expected to be very small compared with the 150,000 lb NERVA lunar payload. Thus, the engine and dual mode system weight should be correspondingly smaller. This is not practical because the nuclear engine weight does not reduce linearly with thrust decrease. Also the need for dual mode electric power may not decrease in proportion to the payload decrease in weight. From these arguments it may be apparent that weight of engine and weight of dual mode system hardware will be at a premium on missions with small payload deep space probes. In general, the method for reducing engine weights is to make the engine smaller (lower thrust) and to reduce complexity (to single propellant feed system).

The dual mode system hardware weight can be reduced by reducing the electric power generating capability and by operating the radiator at a higher temperature. It may be possible to use advanced materials of construction for the radiator - namely Beryllium for the fin which would also reduce weight. Solar Aircraft of San Diego has built beryllium sheet metal structures which have flown on Air Force satellite missions, (9).

In spite of attempts to reduce nuclear engine weight and dual mode hardware weight it can be expected that the relative weights of the payload, engine and dual mode system will be similar. Under these design conditions it is imperitive that payload electrical power requirements be reasonable and that the nuclear rocket engine be considered as a space energy source rather than only a propulsion unit.

If the payload electric power level demand is very small,the size of the radiator which will maximize payload would be determined on the basis of the cooldown fluid saved. In the case of the NERVA Dual Mode System for a lunar mission, the best size was 100 Kw(t) (10 Kw(e)). Reduction of dual mode system size down to 50 Kw(t) (5 Kw(e)) resulted in only a small decrease of payload. Extrapolation of these numbers down to a system with 20% as much thrust suggests that the small,engine dual mode size should be in the range 10-25 Kw(t) or a electrical power capability of 1-3 Kw(e). The values of electric power are small,however, they are in the range required for most deep space probe payloads. The reactor of the engine is expected to be able to supply this quantity of heat readily and there is sufficient surface on the run tank for radiator surface.

There is probably some minimum size dual mode system of practical importance. For example, if the electrical power requirement for a payload is only 100 watts, use of existing systems such as solar cells or radioisotope thermoelectric systems similar to the Alsep units is probably the best choice. For such application chemical rocket propulsion is also expected to be the best choice. As the electrical power requirement goes up the dual mode nuclear system becomes more attractive. At the 3000-5000 watt (3-5 Kw(e)) level the dual mode nuclear rocket system should become very competitive. It is this range in size when the additional weight of the radiator is offset by cooldown fluid savings. Larger systems 10 Kw(e)-50 Kw(e) can be designed but there must be a need for the electric power to make it worth the weight.

The following development plan discussion is based upon a 5 Kw(e) system for the small nuclear rocket engine. The selection of 5 Kw(e) is based upon a compromise power level which will cover most potential needs and a desire to balance a significant portion of the additional weight (mostly radiator) off against cooldown fluid savings and increased engine operational flexibility. An overall system efficiency of 10 percent can be achieved within temperature limits of aluminum as a material of construction for primary and heat rejection loops. The radiator would have a normal operating temperature of 520-540°R, and it could be temporarily operated at 660-700°R to enhance cooldown fluid savings. If the primary loop temperature capability were demonstrated to be in the range of 1000°-1100°R the system would have growth potential to 10-15 Kw(e) using the same heat rejection-radiator circuits.

The approach to the dual mode system has been to work on the system at a slow rate and continue with the nuclear rocket engine development essentially without regard for the dual mode system. Incorporate only those features essential to dual mode in the rocket engine at an early date. This technique reduces to a minimum the additional risks involved to the engine program and it also reduces the amount of redesign and retesting to incorporate the dual mode feature into the engine at a later date.

In the case of the NERVA the essential features for dual mode would have been the heat exchanger flow circuit in the aluminum barrel which surrounded the core. In the case of a core support system heat exchanger the basic design feature is already incorporated. The structure must be leak tight for dual mode; small leakage rate could be tolerated for rocket mode operation only.

Other provisions for the primary flow circuit of the small rocket dual mode system such as backup valves, the venturi the heat exchanger to the pump discharge line are examples of components which can be added to the engine system at a later date with a minimum impact on the engine development program.

The impact of the dual mode system on the stage (run tank) is much greater than it is on the engine. The radiator may be incorporated over most of the exterior surface of the tank. Although this added component (radiator) increases the weight and cost of the nuclear run tank, there is a stage performance gain and an increase in the flexibility of stage operation. These features may be essential to a competitive position for the nuclear stage.

The selection of a definite size for the electrical power of the dual mode system should be delayed as long as convenient. For example, a design range from 5-10 Kw(e) may be a good choice until the total system characteristics are more accurately defined. For the case of the NERVA Dual Mode System, the 25 Kw(e) power level selection was arbitrary but reasonable. The corresponding size for the small engine would be 5 Kw(e).

A rational basis for selection of a system size would balance nuclear rocket stage capability with payload demand. Neither capability or demand are fixed quantities initially. Since a variety of potential payload power requirements exist and the payload community has performed to date with miniscule amounts of electric power it may be prudent to base the initial system size on what is a good size for the nuclear stage. For a small nuclear engine (15,000 lb thrust) this size is expected to be 3-5 Kw(e). If at a later date more electric power is desired, the primary changes requested are in the heat rejection-radiator system. The penalty in weight and performance for over designing the power conversion system by a factor of two is relatively small and the basic incore heat exchanger is already oversized.

The dual mode concept breaks down into three logical units. The nuclear rocket engine with incore primary loop exchanger, manifolding and engine valves is one unit, the power conversion system with boiler (vaporizer) condenser and control system and the heat rejection-radiator system which must be closely integrated with the run tank, are also units. In order to make maximum use of existing capabilities it may be desirable to use three organizations for the development of the dual mode system. One of these organizations or an additional organization should have overall cognizance.

VII. ADVANCED CONCEPTS FOR DUAL MODE

A. Electric Propulsion

One of the often mentioned potential applications for electric power from the dual mode system is to supply an electric propulsion system. Unfortunately, the comparitively large weight of the nuclear rocket vehicle and the relatively low power capability of the dual mode system as described herein makes the use of dual mode electric power impractical for main propulsion. However, the electrical power may be useful for electric thrusters for attitude control of the vehicle during coast phase. The high specific impulse of the electric thrusters and good controlability of this type of system makes this concept extremely attractive.

B. Space Relay Station

The characteristics of a nuclear rocket engine propelled stage are such that a relatively large quantity of propellant (hydrogen) is required for the vehicle to enter a low altitude circular orbit around large planets such as Jupiter. It may be more attractive to eject a small chemical propelled stage into orbit and allow the nuclear stage to fly-by or to retrofire into an ellipical orbit. In either case, the nuclear stage dual mode electrical power system could be used as a power supply for a high power communications relay station between the small orbiter(s) and Earth. The advantage of this concept is that the size and weight of the orbiter(s) can be reduced because the major power supply and communications unit would be on the nuclear vehicle. Thus the nuclear stage would function as a relatively long life, high power relay station.

C. Space Power Plant

A related concept is to connect the nuclear stage to a user by means of an electric wire or cable and fly in formation. This concept was considered as a potential end use for a lunar shuttle nuclear vehible. The nuclear shuttle, complete with positioning thrusters, would be maneuvered to a position in the vicinity of a space station and the two orbiting vehicles would be connected together by means of the electrical cable. The gravity gradient between the two bodies provides a reasonable continuous tensile pull in the cable. A bridle connection at the ends of the cable can keep the alignment constant thus radiation emitted by the nuclear reactor of the dual mode system would not be a problem on the manned station because it could remain in the cone of the shadow shield.

It is also possible that two space vehicles can be flown on a interplanetary flight separated but connected by an electrical cable. Rotation of the two connected vehicles could produce a tensile pull in the cable to maintain a stable, low gravity system.

Limited investigation suggested the need for a reel or spoul takeup unit with damping to make the system function. The weight of electrical cable is quite small even for separation lengths of one mile.

D. Perigee Burns

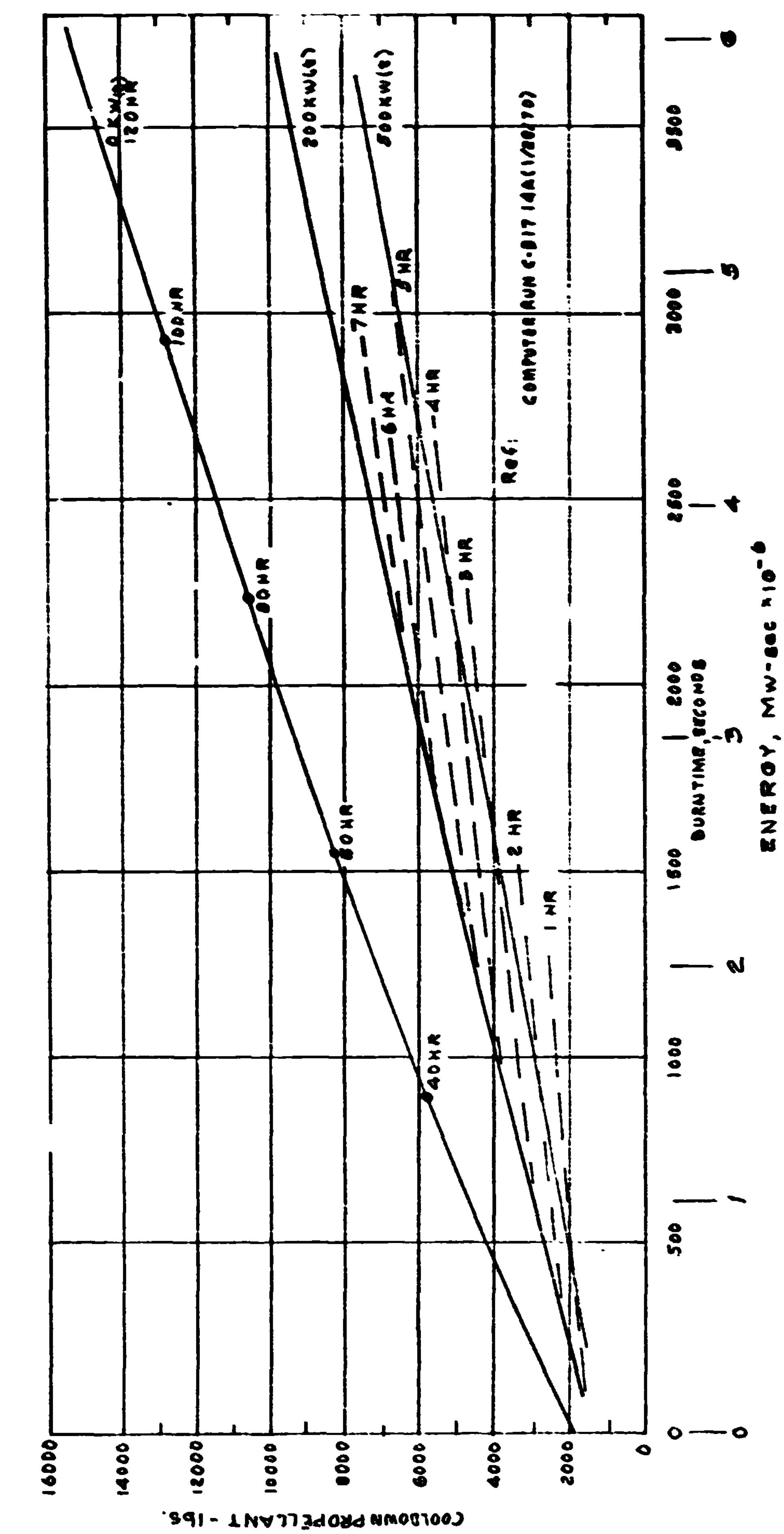
One method of flying a space vehicle on the depart earth orbit manuver is to use "perigee burns". The advantage of multiple perigee burns in comparison with a long single burn is that lower gravity losses are incurred because more of the propellant is consumed at low altitude (and high velocity). The disadvantage of multiple perigee burns for a nuclear rocket engine propelled stage is that the cooldown losses are greater for multiple burns as compared with a single burn. The dual mode system with its radiative means for decay heat rejection reduces the cooldown losses by approximately one-half. As a consequence the trend for a dual mode stage would favor more "perigee burns" and therefore lower gravity losses.

The cooldown propellant requirements for the 75,000 thrust, 1600 Mw(t) NERVA engine were shown in Figure 6 of Reference (1). The units Mw(t) x seconds have been added to the horizontal scale to make the results more general, Figure 13. For a smaller thrust engine, say 15,000 lbs thrust the burn time will be 5 times longer for the same impulse as the larger engine. Since vehicle size is expected to be smaller the actual burn times will be similar to those expected for the NERVA applications.

The Figure 6 of Reference (1) also showed lines of constant time after shut to reach a 200 Kw(t) and 500 Kw(t) residual thermal power levels as a function of burn time for the NERVA engine. These same times should represent an upper limit for the smaller engine. Actual times for the smaller engine should be less because for the same megawatt-second value the smaller engine would have run longer (5x) and some of the earlier formed fission products will have decayed during the run.

Figure 13

Cooldown Propellant Consumption and Duration Vs. Burn Time for 0, 200, 500, kw(t) Decay Heat Removal Systems



E. Hot Run Tank

The dual mode heat rejector system requires a relatively large radiator surface for rejection of waste heat. For the NERVA Dual Mode concept, the temperature of the radiator was 520-540°R. The only area conveniently available is the outer surface of the propellant tank. Thus, the radiator design was integrated with the meteoroid protection skin which surrounded the propellant tank. Super insulation separated the ambient temperature radiator from the liquid hydrogen in the tank.

For the small engine attached to a "run tank" it would be desirable to use the outer surface of the run tank for the radiator. This would be feasible if during the initial burn all of the propellants were consumed from the run tank. Subsequent burns, if any would be supplied propellants from propellant modules above the run tank.

This concept has great merit for the dual mode system because the integrated radiator is very difficult to thermally isolate from a tank of liquid hydrogen when the radiator surrounds the tank. Use of the electrical power from the dual mode system to pump out the heat input due to the radiator is not attractive.

Inherent in the hot run tank concept is the integration of the dual mode system with the run tank. This makes it feasible to completely assemble and checkout the dual mode system prior to launch and potentially leaky joints are avoided.

F. Heat Pipe Radiator Surface

Methods for making the heat rejection loops less sensitive to meteoroid damage and loss of coolant failure were investigated. One approach is to use arrays of heat pipe surfaces to spread the heat from the flowing hydrogen coolant tubes to the fin material between coolant tubes. Heat pipes are very desirable for this application because they greatly increase the effective conductivity of the fin material, thus greatly reducing the number and plan form area of the coolant distribution tubes. Loss of individual heat pipes would not be of great importance because there would be a large number of them in parallel and the heat load of adjacent heat pipes could pick up the heat load of inoperative units. To reduce costs, the development of a heat pipe skin material for the radiator surfaces may be necessary.

G. Modular Dual Mode System

The engine flow schematic, Figure 3, shows the primary coolant loop lines incorporated into the main engine flow circuit in a manner which may permit effective power generation during normal engine operation during cooldown and post cooldown coast phase. The primary coolant loop lines do not have separate valves, hence it is imperative that the primary loop maintain its leak-tight integrity during all modes of engine operation. To insure a leak-tight system, it is desirable to eliminate valves and to have all welded or brazed joints in the piping. To accomplish this objective, the primary coolant lines from the engine pressure vessel, across the gimbal axis, up the side of the run tank and at the vaporizer must be an integral piping system. This can be accomplished with a single engine-run tank flight system which is launched as a complete system.

If additional propellant modules are included or added to the basic vehicle and if radiator surface area is incorporated on the additional tankage, a leak-tight thermal connection between modules is required. This may be done with a thermal connector utilizing heat pipes which fit together and conducts heat across the joint. In this way individual-separate tank modules can be thermally connected without requiring leak proof gaseous hydrogen flow circuit connection(s) to be made up in space. Each module would have its own closed heat rejection radiator flow circuits.

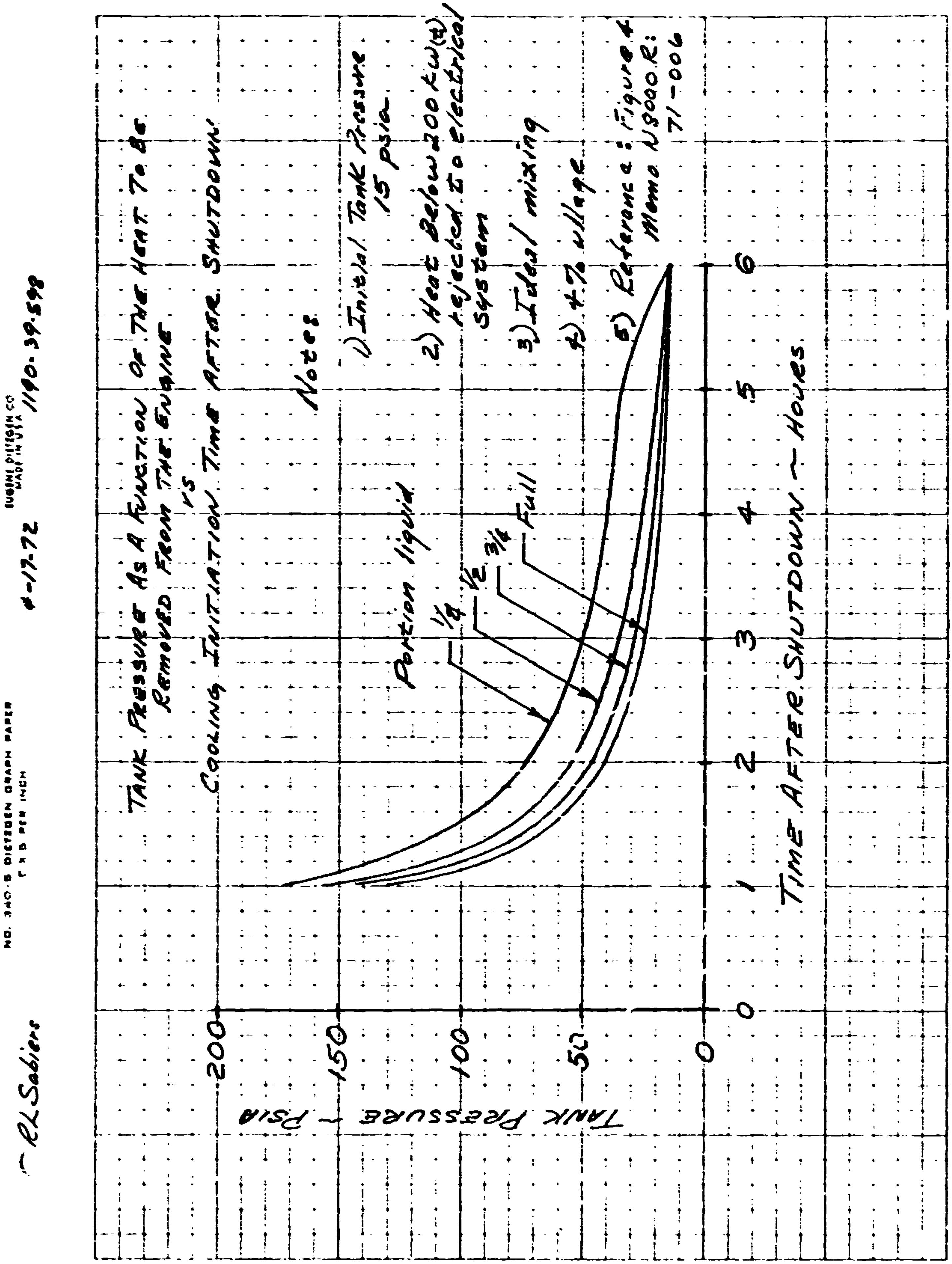
H. Propellant Tank Thermal Storage Capacity

The hydrogen in the propellant tank has a considerable heat storage capacity. To illustrate this potential, the cooldown heat rejection rates of the NERVA engine were used to determine how tank pressure would vary if all the heat required to be removed from the engine were transferred into the hydrogen. The resulting vapor pressure rise will, of course, be a function of how much propellant is in the tank at the initiation of cooling.

Figure 14 plots the terminal tank pressure as a function of how long after engine shutdown that the cooling was initiated. It is interesting to note that it is possible to absorb the later three hours of cooling into the hydrogen propellant for tank capacities of one-half full or more without raising the tank pressure above 35 psia.

With the advent of "zero NPSH" pumping capability, it is believed that some consideration of propellant storage for cooling requirements should be made on future vehicles. The propellant could also be cooled down over a longer time period by a refrigerator powered by a Dual Mode electrical generating system. In addition, the tank will be cooled and thus depressurized if it is kept "locked-up" during engine operation.

Figure 14



TANK PRESSURE AS A FUNCTION OF THE HEAT TO BE REMOVED FROM THE ENGINE
VS
COOLING INITIATION TIME AFTER SHUTDOWN
Notes
1) Initial Tank Pressure 15 psia
2) Heat Below 200 kw(t) rejected to electrical system
3) Ideal mixing
4) 4% ullage
5) Reference: Figure 4 Memo N8000:RI 71-006
Portion liquid
1/4
1/2
3/4
Full
TIME AFTER SHUTDOWN ~ HOURS
0
1
2
3
4
5
6
TANK PRESSURE ~ PSIA
0
50
100
150
200
4-17-72
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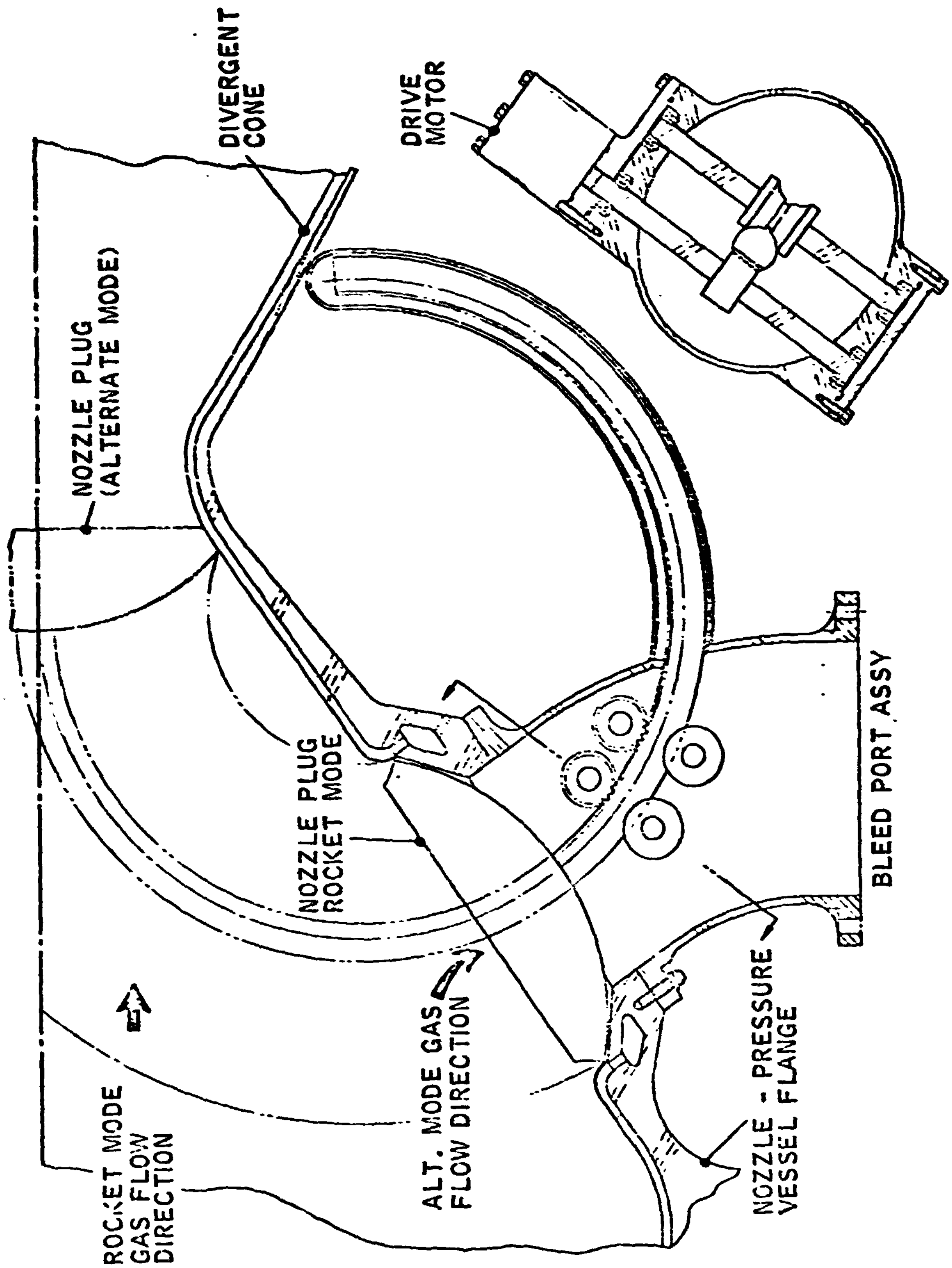
~RLSabiers

I. High Power Dual Mode System

The dual mode concept described throughout this report is applicable to a closed cycle space electric power plant useful for extended operating period. Open cycle dual mode systems which utilize reactor power from the rocket engine are also potentially useful. Open cycle dual mode systems would utilize the hydrogen pumping system, which is an integral part of the rocket engine, to supply hydrogen at high pressure to the reactor. The heated hydrogen could be used to run a turboalternator or the very high temperature hydrogen could be expanded through a magneto hydrodynamic duct attached to the rocket exhaust nozzle.

The open cycle electric power systems may be incorporated without eliminating the rocket engine thrusting potential. For example, a technique for driving a turboalternator is shown in Figure 15. A swing poppet valve normally retracted into the convergent section of the nozzle is used to close off the rocket nozzle throat and open a bleed line which leads warmed hydrogen to drive the turboalternator. The positive shut-off valve in the bleed line is located near the turbine, thus the shut-off valve is out of the high radiation dose region adjacent to the nozzle throat. A small leakage rate through the poppet valve at the nozzle throat is not serious because it cannot burn out the nozzle and the thrust produced by the leakage is small compared with the turbine exhaust thrust.

Use of the nuclear rocket engine for an energy producing device is quite attractive because the quantity of hydrogen required for each unit of power produced is very small compared with other working fluids. The advantage of hydrogen for an energy producing system is even greater than it is for a



NERVA NOZZLE - BLEED PORT (ALTERNATE MODE) thrust producing system. This characteristic tends to make the high power dual mode system most attractive for relatively long duration applications. It should be mentioned that the application should be continuous. If off-on operation is required, then cooldown flow requirements between operating cycles must be included.

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